



ASSESSMENT OF EMERGENCY
CORE COOLING SYSTEM
EFFECTIVENESS FOR LIGHT WATER
NUCLEAR POWER REACTORS

by
FRED C. FINLAYSON

EQL REPORT No. 9
May 1975

Environmental Quality Laboratory
CALIFORNIA INSTITUTE OF TECHNOLOGY
Pasadena, CA 91125

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ABSTRACT

ASSESSMENT OF ECCS EFFECTIVENESS FOR LIGHT WATER NUCLEAR POWER REACTORS

The effectiveness of Emergency Core Cooling Systems (ECCS) for light water nuclear power reactors was the subject of lengthy, controversial and technically complex hearings conducted by the AEC over the two years from 1971 through 1973. An independent, objective review and assessment of the technical issues associated with ECCS effectiveness was conducted in a study performed at the Environmental Quality Laboratory of the California Institute of Technology. The review was based upon the testimonies and supporting technical documentation of the principal participants in the hearings: the AEC, utilities, reactor manufacturers, and intervenors.

From the review, the critical technical parameters influencing ECCS performance, which were at issue, are identified. Of fifteen parameters cited by the Advisory Committee on Reactor Safety in the hearings as being of unproved conservatism, essentially all are reviewed in detail, including, for example, the initial stored fuel energy, fuel rod gas gap conductance, fluid flow rates through broken pipes, metal-water reaction energy release and fuel rod embrittlement, reflood/core-spray heat transfer, and reflooding rates, as well as the adequacy of ECCS analytical models and numerical methods. The relative influence of uncertainties in the performance criteria associated with these parameters is assessed. Based upon the relative importance of these parameters, alternative responses to resolution of the ECCS problem are analyzed. The importance of the core reflooding rate in resolving the technical issues of the problem is emphasized. The conservatism of the proposed criteria (current and past) is reviewed. Recommendations are made for improvements in criteria conservatism, especially in the establishment of minimum reflood heat transfer rates (or alternatively, reflooding rates). Several new and/or accelerated research programs and additional large scale testing programs are also recommended. Suggestions are also made for areas in which design improvements would help to achieve greater ECCS reliability.

FOREWORD

The siting of nuclear power plants in California is one of the problems which the Environmental Quality Laboratory has addressed. Previously published work dealt only with the siting issue, and not with the question of the desirability of nuclear power plants or their problems. But at the outset it was recognized that at least two major technical problems pervaded all public discussion of nuclear power. One was the question of disposal of high-level radioactive waste; the other was the adequacy of plant safety systems and particularly the emergency core cooling system (ECCS). While one can study the siting of power plants without reference to the former problem, the latter problem can enter into consideration of specific site locations.

Because EQL studies, as well as those performed by other groups, have shown that many sources of energy will be needed to meet society's perceived needs, it has been our view that nuclear power plant siting and safety problems are in urgent need of resolution. The Laboratory staff would have preferred not to address the safety question, but we found that we could not consider siting without facing the public's questions on safety. We also recognize that the nature of EQL would preclude our adding to the massive body of theoretical and empirical knowledge concerning reactor engineering. It was felt, however, that we should understand the nature of the controversy in order that we could at least communicate the facts to those interested.

It is our intention to consider the problem of radioactive waste disposal in future studies. The present study, carried out by Dr. Finlayson, addresses one key element of the power plant safety problem. In the recently completed study of reactor safety sponsored by the AEC¹ (termed the Rasmussen Study after the principal investigator), analysis was made of the probability of accidents of various types, and estimates

¹ WASH 1400 Reactor Safety Study (Draft), USAEC, August 1974.

were made of the likely consequences of such accidents. That study did not analyze the physical events occurring during the "maximum credible accident," or loss-of-coolant accident (LOCA). The Rasmussen study concluded that the probability of accident was very low, and that the expected consequences were far less than previous "worst case" analysis would suggest.

Previously, the issue of the physical events transpiring during a LOCA was the subject of extensive hearings on the Interim Acceptance Criteria for Emergency Core Cooling Systems. Subsequent to the hearings, revised Acceptance Criteria were issued by the AEC and are in force. The continued public debate over the safety of nuclear power plants centers on the adequacy of the Acceptance Criteria.

This study by Dr. Finlayson (which was carried out while the hearings were being completed and afterwards) centers upon the physical events of a LOCA and the adequacy of the Acceptance Criteria for insuring successful design of the ECCS. (This is different from the Rasmussen analysis, which investigated the probability of the equipment working as it should.) The purpose of this study is informational.

The technical facts, as reported in the literature and reviewed in the AFC hearings, are summarized and highlighted. From this, Finlayson has been able to differentiate alternative courses of action for reducing perceived hazards associated with ECCS operation. Comments have been provided on the results that might be expected from following one route or another. Among the alternatives are those which would be painful or unacceptable to one or another point of view. There are paths, however, which may more closely approach acceptability to all. It is hoped that by publishing this document the participation of an informed public in the process of decision-making will be helped.

The importance of "stopping the argument" is sometimes lost to partisans of both sides in the controversy over nuclear power. It is instructive to reflect on the arguments that have raged over the appropriate levels to

be required for limits on radioactive emissions from power plants during normal operation. The controversy was effectively stilled when the proposal was made by the AEC to reduce allowable limits to the point where the acrimonious, detailed technical arguments were no longer pertinent, yet the cost factors involved with the rules were (hopefully) not too onerous to the operators. The question of routine emissions is no longer much of an issue.

In the same way Finlayson has sought technical solutions which avoid many of the detailed arguments yet hopefully can be implemented at a cost within reason. It is in this spirit that the study has been published.

Martin Goldsmith
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Basic to an understanding of the controversies which have surrounded some nuclear plants is the realization that from the inception of the nuclear power program, the [Atomic Energy] Commission has been concerned with safety. For many years that was practically the only issue considered at the public hearings held in local communities on individual plants. Many persons within the nuclear industry have commented that the AEC talked about safety so much and is supporting so much safety-related work that it is not surprising that the average citizen may have some apprehensions.

The jargon of the nuclear industry has not offered much comfort. Terms such as "design basis accident," "maximum credible accident" and "reasonable assurance" may be perfectly acceptable to the scientist and engineer, but are not reassuring to the public. On the other hand, some persons who willingly accept everyday risks such as highway traffic, walking across streets, using electricity and fire, often use another yardstick with respect to nuclear power, insisting on absolutes which will never be attainable (U. S. Atomic Energy Commission, Dec. 1972, (1, p. III-1)*.

We now approach the key issues. The reactor constructors claim that they have devoted more effort to safety problems than any other technologists have. This is true. From the beginning they have paid much attention to safety and they have been remarkably clever in devising safety precautions. This is perhaps pathetic, but it is not relevant. If a problem is too difficult to solve, one cannot claim that it is solved by pointing to all the efforts made to solve it (Hannes Alfvén, May 1972, 2).

It is generally conceded that the nuclear power industry has expended more effort to insure the safety and reliability of operating reactors than has ever been expended by any other industry in safety related activities. However, in spite of their efforts, substantial controversy has recently developed over nuclear plant safety. Members of the nuclear community have suggested that the probability of a

* Underscored numbers refer to references. See reference list at end of report. (Appendix 11)

major accident involving the potential release of large quantities of radioactive fission products is extremely small, the probability being of the order of one accident in 100,000 to 1,000,000 reactor years of operation (3). Although the quantitative value associated with the probability of a major reactor accident is a subject of controversy, it is generally conceded that the probability is very small. However, even accepting the low accident probability there are a sufficient number of people, as represented by Alfvén, who feel that unresolved safety issues still exist, in spite of the good efforts of the reactor engineers and scientists, to make reactor safety a significant current national problem.

It is the goal of this paper to try to put into perspective one aspect of the safety of light water power reactors (LWR), the functional effectiveness of the so-called Emergency Core Cooling System (ECCS). The ECCS is the element of a nuclear power plant which is designed to cool the reactor in the event of one of the most serious possible accidents considered credible by the U. S. Atomic Energy Commission (AEC) -- the so-called Design Basis Accident (DBA) -- the Loss of Coolant Accident (LOCA).

This report represents the results of an investigation of the problem areas associated with the ECCS reliability issue. An attempt has been made to present an objective evaluation of the principal areas associated with the ECCS controversy, and to provide an evaluation of the options available to produce at least partial resolution of some of the apparently unresolved issues. In order to do this, a brief review is given in this chapter of the philosophy and practice in the design of nuclear power reactors, as promulgated by the AEC.

The principal technical issues associated with the ECCS controversy have been raised and discussed in advisory hearings conducted by the AEC. This report presents the results of an attempt

to weigh the technical evidence presented directly or indirectly in connection with the hearings. The principal protagonists in the adversary hearings and their primary presentation of technical material were: the AEC regulatory staff, who presented (as a partial listing) an initial statement of Testimony (8), a Supplemental Testimony (4), a concluding Statement (6), and an Environmental Statement (61); the reactor manufacturers, each of whom submitted a similar number of testimonial elements into the record (e.g. 21, 27); the electrical power utilities (22); and a combined group of intervenors, the Consolidated National Intervenors, whose principal technical spokesmen were representatives of the Union of Concerned Scientists, Daniel Ford and Henry Kendall (5,7,9). In the course of this review, the testimonies and statements of these organizations were reviewed in depth and supplemented by evaluation of many other supporting documents, most of which have been noted in the reference list.

1.1 Reactor Safety in Perspective

In attempting to assure the safety of nuclear power generation the AEC has promulgated a design philosophy of multiple barriers against the escape of radioactivity from nuclear facilities.* The AEC describes this as the "defense-in-depth" design philosophy embodying "three levels of safety." These three levels of safety are described by the AEC as:

The First Level of Safety

Precept: Design for unquestionable safety in normal operations and maximum tolerance for system malfunctions. Use design features inherently favorable to safe operation; emphasize quality, redundancy, inspectability, and testability prior to acceptance for sustained commercial operation over the plant lifetime.

* Basic principles of light water reactor and emergency core cooling system operation and design are presented in appendix 1.

The Second Level of Safety

Precept: Assume accidents will occur in spite of care in design, construction and operation. Provide safety systems to protect operators and to prevent or minimize damage when such accidents occur.

The Third Level of Safety

Precept: Evaluate effects of hypothetical accidents, where protective systems are assumed to fail simultaneously with the accident they are intended to control. Provide additional safety systems as appropriate (1, pp. 2.2 to 2.5).

The first level of safety embodies the concept of selecting fuel, coolant and structural materials whose properties are well known and incorporating them into designs which have inherent stability and safety characteristics. The philosophy calls for safety margins (i.e., conservatism in thermal, hydraulic and structural member design) to be incorporated into designs at all critical stages. Instrumentation and controls are to be provided to assure that operators know the operating conditions of the plants at all times and have control over them. Redundancy is a recommended characteristic of design in all crucial safety related areas -- including instrumentation -- to assure that the failure of one component will not compromise the safety of the entire system or deprive the operators of needed information to ensure its safe operation.

The second level of safety represents a recognition that in spite of all efforts to insure a totally safe design, failures, design errors, construction oversights and operating errors will occur in the course of the lifetime of the plant. This level is designed to provide safety systems to accommodate a spectrum of possible mistakes or oversights before they become accidents in which the risk of public radioactive contamination is experienced. As an example, redundant offsite power sources needed to energize emergency equipment in the

event of loss of the plant's own power are backed up by redundant on-site power sources. A fast-acting reactor shutdown (SCRAM) system is provided. The SCRAM system is designed to terminate the nuclear fission process in the event of emergency conditions. It is activated by redundant and independent instrument channels which monitor plant parameters. Engineered rate-limiting mechanisms are built into the system to prevent excessive rates of power increase which might result from abnormal motion of the control rods or their accidental ejection from the core.

The third level of safety supplements the first two by providing additional safety systems to cover the consequences of potential, although highly improbable, combinations of failures of protective systems of the first and second safety levels. The margin of safety provided in this third level is evaluated by analyzing the system response to the so-called design basis accident, the most severe accident which is considered conceivable for design purposes. The DBA is an accident which is postulated to occur at a time when a single element of the safety system is also temporarily (or permanently) unavailable (the so-called single-failure criterion). The failed element of the system has been determined to be the one which results in the most serious system consequences. For light water reactors, the design basis accident is the so-called Loss of Coolant Accident (LOCA).*

1.1.1 LOCA consequences in the event of ECCS failure

The LOCA is assumed to occur as a result of the rupture of one of the main coolant pipes for the system and to result in a sudden loss of reactor coolant water with accompanying rapid nuclear steam system depressurization as the fluid is exhausted into the containment vessel. This period is referred to as the "blowdown" phase of the accident. In a LWR, the cooling water is an integral part of the

* A detailed presentation of the physical processes occurring during a LOCA is presented in appendix 2.

nuclear reaction process, acting as a moderator which slows the fissioned neutrons to velocities such that the potential for further fission in the enriched uranium fuel is enhanced. The loss of the coolant in the reactor core will stop the nuclear reaction, which is the source of energy for power generation. Consequently, uncontrolled nuclear excursions or bomblike explosions are not possible consequences of a LOCA.

However, the nuclear reactions of normal operations produce long-lived radioactive fission products within the fuel rods (the nuclear equivalent of ashes from fossil fuels) which continue to release substantial quantities of highly energetic radiation by radioactive decay even after the nuclear fission process has been stopped. The fission product inventory of a typical reactor is described in more detail in appendix 1. Exposure to many of the fission products is extremely hazardous. The AEC's "three levels of safety" concept was developed basically to prevent fission products from being released into the environment. The potentially hazardous result of a LOCA is that it may result in all safety barriers between man and fission products being broken down.

To prevent the escape of fission products into the environment, the ECCS must be able to cope with the energy released. Immediately after plant shutdown, following a period of sustained operation, the radioactive decay of the fission products will release energy equivalent to about 7 percent of the rated thermal output of the plant. For a nuclear power plant producing 1000 megawatts of electrical power (ME_e), approximately 3300 megawatts of thermal energy (MW_t) are produced under normal operating conditions by the reactor, assuming a typical efficiency of approximately 33 percent. For such a reactor, immediately after shutdown 225 MW_t of heat would be produced as a result of the radioactive energy of the fission products. The energy output of the fission products decays rather rapidly to 5 percent at 10 seconds

after shutdown, 2 percent after about 10 minutes, 1 percent after 2.25 hours, until within a day after shutdown only approximately 0.5 percent or about 15 MW_t, of the rated power is produced (61, p. 20). Because this energy results from radioactive disintegration (or decay) of the fission product nuclides, this heat source of the reactor is commonly called "decay heat."

Although the relative magnitude of the decay heat is small when compared to the rated output of the plant, its absolute magnitude is sufficiently large that it requires active cooling for long periods to prevent meltdown with subsequent catastrophic results. In the event of a LOCA, unless supplementary cooling water is supplied quickly to the fuel rods they will rapidly increase in temperature with consequent swelling and rupture. The function of the Emergency Core Cooling System is to supply this needed cooling water to the reactor core to prevent excessive fuel rod damage. However, in the event of a design basis LOCA, even with adequate ECCS performance, some fuel rod rupture would probably take place. In the words of the AEC Commissioners, as stated in their Opinion to final ECCS Acceptance Criteria (AC),

...it is obvious that, when the course of the LOCA is calculated according to the conservative prescriptions of an approved evaluation model, swelling and bursting of the cladding will be estimated to occur in abundance (60, p. 1105).

The ruptured rods would release the majority of their gaseous and volatile fission products to the reactor containment and possibly (even assuming no major failures of reactor containment vessels) to the environment thereafter at a low leak rate. Fuel rod rupture alone would not be considered catastrophic, if it were to occur.

But if the ECCS should fail to function, the absence of coolant would cause the rod temperatures to increase rapidly. Without cooling, the redistribution of the internal energy of the fuel rods alone would cause the surface temperature of the hottest

rod in the reactor to increase to about 2300°F. In the continued absence of coolant, due to the coolant pipe rupture, and the ECCS failure, the most highly irradiated rod in the core would increase in temperature at a rate of about 20°F per second. At rod temperatures on the order of 2000°F (and higher), exothermic reaction would take place between the remaining steam vapors in the core and the fuel rod cladding material itself (commonly a zirconium alloy, zircaloy). In addition to adding energy to that of the decaying fission products, these reactions produce hydrogen which may induce a potentially explosive environment when mixed with air in the containment vessel into which the coolant, hydrogen from the reaction, and gaseous and volatile fission products are discharged from the ruptured pipes. Moreover, this metal-water reaction produces oxidation of the fuel rod cladding which may induce its embrittlement. In the event of excessive oxidation, the cladding may become so brittle that the loads developed during cooling could cause them to disintegrate with subsequent dispersal of fission products to the containment vessel and potential blockage of coolant paths within the reactor core.

As the uncooled LOCA thermal excursion continues, at temperatures on the order of 3400°F, melting would occur. After a reasonably short period of time (estimated to be from 10 minutes to one hour, 11, p. 141), the molten fuel could be expected to have collapsed into a heap in the bottom of the pressure vessel and then through the bottom of the containment vessel into the earth with resultant great increases in the subsequent potential for dispersal of large quantities of radioactive fission products in the biosphere. The molten mass of core material could then proceed to melt its way into the earth transferring energy to the earth and decaying in energy itself with time, as it slowly progressed downward. Its downward progress would ultimately be limited by achievement of a stable condition in which the rate of energy production of the molten mass matched the

heat transfer capacity of the surrounding rock. The concern over the downward migration of the core material into the earth has sometimes been referred to as the "China syndrome," indicative (in an exaggerated fashion) of the uncertainty concerning the terminal depth of the molten core material. In point of fact, such progression would probably stop within some hundreds of feet.

The disastrous potential of the sequence of events which might occur in the event of ECCS failure, as outlined above, is clear. The release of a small fraction of the gaseous and volatilized fission products of a large power reactor to the environment surrounding the plant could have several effects in the vicinity of the plant. The wind-blown radioactivity might be expected to produce, depending upon exposure levels and the magnitude of the release, prompt deaths (within 30 days) from acute downwind radiation exposure, long-term health effects (both somatic and genetic) from lower levels of radiation exposure downwind, and property damage, perhaps most importantly denial of the agricultural and other kinds of land use for long periods.

1.1.2 LOCA probability

Although the probability of an accident occurring leading to the loss of coolant is considered extremely remote, Emergency Core Cooling Systems (ECCS) have been designed and built into existing power plants to preclude the problems that would be associated with a LOCA in which no cooling water was delivered to the core. If the ECCS performs in accordance with design, coolant will be supplied to the core by spraying or flooding so that excessive fuel rod temperature increases, along with consequent gross deformation of the core, are prevented. The system is also designed to assure that the long-term decay heat associated with the core will be adequately removed to prevent subsequent failure occurring through the previously described series of events.

It has been stated by the AEC that the probability of the design basis accident LOCA occurring at all is remote (i.e., on the order of one accident in 100,000 to 1,000,000 reactor years, as previously noted). However, even if the validity of such assertions is accepted, the probability of a LOCA occurring is still finite, and the consequences of failure of the ECCS to function are so severe that it is necessary to require that reliable ECCS performance be a high probability event.

It is essentially impossible to develop a statistical data base for such low probability events as the LOCA. Consequently the public, and much of the scientific community at large, is inclined to mistrust the use of probabilistic estimates as a basis for conclusions with respect to ECCS performance, in connection with the broader questions of nuclear reactor safety. However, LOCA probability is generally conceded to be very low by the technical community, even though estimates of its quantitative value may be uncertain.

There is sometimes a tendency to allow the apparent low probability of the DBA to influence the evaluation of the importance of the ECCS. For example, Stephen Hanauer, chief technical advisor to the AEC's regulatory staff, has stated, "In principle, it should be possible to reduce the probability of a LOCA to so low a value that protection against its consequences - the ECCS - would not be required" (12). However, it is the opinion of many, the AEC regulatory staff included, that the inability to guarantee a zero probability for the LOCA and the magnitude of its consequences without emergency cooling make it essential that the ECCS must exist and perform reliably.

Consequently, and importantly, the probability of successful performance of the ECCS should be considered as a separate issue from the probability of the LOCA itself. Otherwise, the two events tend to

become confused. As much weight then begins to be given to the unlikelihood of the LOCA occurring as is given to evaluation of the technical and scientific phenomena behind the design of the ECCS itself.

That this concept is recognized and followed is demonstrated in statements by Milton Shaw, the AEC's former director of Reactor Development and Technology (RDT) and Andrew J. Pressesky, the Assistant Director for Nuclear Safety in the RDT at the time of the ECCS hearings. "Our job is to work out these problems," Pressesky says, "and that's what we're trying to do. For our purposes, the probability of an accident is one" (emphasis added). Shaw adds that he thinks "serious reactor accidents will inevitably occur - but that safety systems will protect public life and property" (13). Because of the non-zero probability of serious accidents, as acknowledged by Shaw and Pressesky, our study has been conducted under the implicit assumption that a LOCA can occur. As was the case in the hearings, only the reliability of ECCS' performance is the subject of evaluation.

1.2 Historical Assessment of the Bases for the Interim Acceptance Criteria

The preliminary set of performance standards -- or Interim Acceptance Criteria (IAC) -- against which ECCS were to be designed were the subject of lengthy (125 days from Jan. 27, 1972 to July 25, 1973) hearings before the AEC. The expectations for the hearings were summarized in the words of Alvin M. Weinberg, then Director of the AEC's Oak Ridge National Laboratory (ORNL). Dr. Weinberg stated: "Faced with questions of this weight, which in a most basic sense are not fully susceptible to a yes or no scientific answer, the AEC has invoked the adjudicatory process The record of the hearings is expected to contain all that is known about emergency core cooling systems and to provide the basis for setting the criteria for design of such systems" (14, emphasis added).

Perhaps the judicial concept of adversary proceedings selected by the AEC for the hearing, pitting the regulatory staff against the Consolidated National Intervenors, as well as the reactor manufacturers and electrical utilities, may not have been the most practical means of producing the result hoped for by Weinberg and the public at large. The hearings required 125 days of testimony and cross-examination over an 18 month period. The results are contained in 22,380 pages of recorded transcript of oral testimony in which more than 1000 documents were referenced, of which about 250 were admitted to the record as exhibits.

Why were the hearings required and what was the basis for issuance of the controversial IAC? The IAC represented a major milestone in over five years of continuing activity by the AEC concerning ECCS. Around 1966, the AEC's regulatory staff became concerned about problems associated with extrapolation of the design and analysis procedures for the small nuclear power reactors of that period (generally of less than 100 MW_e capacity) to today's very large plants with capacities frequently greater than 1000 MW_e, which were then in the planning and initial construction phases. Research was especially sought on information related to the emergency core cooling problem. A task force of 12 engineers and scientists (seven from industry and five from AEC supported labs), headed by the late William K. Ergen of ORNL, was appointed in October 1966 to investigate the problem. In 1967, the Ergen report, "Emergency Core Cooling" (11), was published with a limited distribution. The report pointed out some serious issues demonstrating the need for effective ECCS operation and outlined a recommended major research program to resolve the uncertainties highlighted by the investigation.

It does not appear that all of the programs recommended by the Ergen task force were ever implemented. But several major changes in the LWR safety research program did occur as a result. As a

particular example, in 1967 the AEC reoriented a major research reactor program then under construction in recognition of the importance of investigating ECCS operational phenomena. The project, called the Loss of Fluid Test (LOFT) facility, had originally been conceived to investigate the effects of the meltdown of a reactor following a LOCA, when no ECCS was in operation. Shaw redirected the emphasis of the program to the more difficult problem of answering questions associated with the physical processes of the ECCS cooling of a reactor which has undergone a LOCA. Unfortunately LOFT ran into prodigious cost overrun problems and delays as a result of this change in design concept and the program's being made a showcase for demonstration of the AEC's then newly emphasized quality control program. As a result, the LOFT facility is still under construction and test schedules are uncertain. System tests under nuclear power are not scheduled until 1976.

In spite of the urgent need for accelerated research on reactor emergency cooling, the AEC's safety budget remained essentially constant from 1967 to 1972 at approximately \$35 million per year. To compound the painful aspect of budgetary restraints, it should be noted that within this period of constant budgets, where inflation meant reduced research activity at best, the fast breeder's share of this safety budget rose from \$4 million in 1969 to more than \$11 million in 1972. Though Shaw fought to maintain or increase the safety budget, it was the opinion of Congress and the budgetary elements of the executive branch that LWRs had matured sufficiently to permit industry to handle the bulk of their own safety related R&D.

The AEC's limited safety research funds resulted in discontinuance of important programs related to several aspects of ECCS problems which were nowhere near completion. As an example, in February 1971, an ORNL program felt by many to be the lab's single most important piece of nuclear safety research, a study of how reactor fuel rods might behave during a major LOCA, was cancelled, though it had run only two of its scheduled four years.

In the midst of these painful budget exercises, two additional storm warnings were raised for the AEC. In 1969, an AEC Internal Study Group provided results of their work calling for greater emphasis on quality control in reactor design and construction and confirming the use of the LOCA as a design basis accident. This study was followed in a letter from the ACRS, of 12 November 1969, which reemphasized to Glenn Seaborg, then Chairman of the AEC, the ACRS' concern over the neglect of research on emergency core cooling and fuel failure mechanisms for LWRs. The letter stated in part:

The committee has strongly recommended safety research of this kind several times during the past three years; the regulatory staff has also strongly supported such work. However, only small or modest efforts have been initiated thus far (13).

So, it became increasingly evident to the AEC that a need existed to perform more LOCA research. Thus after over two years of review, in February 1970, the AEC published their "Water Reactor Safety Program Plan" (WASH-1146) (15). The plan outlined 139 unsettled safety questions and designated 44 of them (including many related to the ECCS) as "very urgent, key problem areas, the solution of which would clearly have great impact, either directly or indirectly, on a major critical aspect of reactor safety" (15).

In November 1971, WASH-1146 was followed up by a supplemental "Water Reactor Safety Program Augmentation Plan" (16). This document emphasized:

Emergency core cooling has been described in the overall Program Plan, WASH-1146, as 'generally considered to be the most urgent problem areas in the safety program today.' A major loss-of-coolant accident is extremely unlikely, and present ECC systems, as designed, are expected to mitigate the consequences of such an accident should one occur. However, present experimental data and analysis techniques are not now sufficient to provide the degree of ECC assurance deemed necessary by the AEC [emphasis added] (16, p. 7).

In the midst of this environment, late in 1970, a "senior task force" of four executive members of the AEC's regulatory staff, under the direction of Stephen H. Hanauer, was appointed to evaluate the ECCS problem. Perhaps it is no coincidence that almost at the same time the task force was appointed some dramatic experimental results were obtained which appear to have had an important impact on development of the IAC. From November 1970 to March 1971, a series of experiments were conducted at the AEC's National Reactor Testing Station (NRTS), Idaho Falls, Idaho. These experiments, designated the "Semiscale Blowdown and Emergency Core Cooling (ECC) Project" were conducted on a highly idealized model of a Pressurized Water Reactor (PWR) as part of the research program supporting and leading to the LOFT program. The "Semiscale" tests were conducted using a 9-inch mock up of a reactor pressure vessel containing electrically heated simulated fuel elements cooled by water circulating through a single-loop heat exchanger circuit.

In a series of tests with the apparatus described above, it was discovered that in simulated LOCAs essentially all of the emergency core coolant injected following initiation of the break was swept out of the reactor mock up (along with the original coolant water) during the rapid decompression -- or blowdown -- phase of the simulated accident. The complete and total expulsion of the emergency coolant apparently came as a most unpleasant surprise to the researchers and AEC. Since critics of nuclear power have tried to use the "Semiscale" test results to "demonstrate" that adequate ECCS performance is unlikely, it should be reemphasized that the test equipment was substantially different from an actual operation nuclear steam supply system (see appendix 1). One of the principal differences was the use of a single loop for the primary reactor heat transfer system for the test apparatus. Operational nuclear power plants all use multiple loop heat exchange paths between the reactor and two to four steam generators -- as opposed to the single-loop of the "Semiscale"

equipment. Thus, among other things, the coolant flow path redundancy helps to reduce the probability that all of the emergency coolant would be expelled during blowdown. Direct extrapolation of the results to operational reactors are clearly invalid. Nonetheless, the results were unpleasant because they were unexpected. Analytical methods applied by those conducting the experiments to the experimental apparatus had not predicted the outcome as it took place.*

Apparently in response to the "Semiscale" results, George M. Kavanaugh, the AEC assistant general manager for reactors, appeared before the Joint Committee for Atomic Energy on 13 May 1971 to request supplemental funding for several "significant technical issues" -- including \$2 million for water reactor safety. In the course of his presentation, Kavanaugh was questioned about a statement he made implying that the "Semiscale" results had not "resolved some of the areas of major uncertainty raised by differences among the analyses (furnished by reactor manufacturers) particularly with regard to their evaluation of the operating effectiveness of the emergency core cooling" (18). When asked by Senator Howard Baker (R. Tenn.) to explain what he meant by "differences," the following dialogue took place:

Kavanaugh: ". . . . [The experiments] have had results which have not been confirmatory of what the people doing those experiments thought might happen. Now they are not conclusive. . . ."

Baker: ". . . . meaning that it was worse than you thought?"

Kavanaugh: "Yes, worse. If it were better we might not have been allowed to come up here asking for money. But they [the results] are not conclusive. In other words,

* Analytical evaluations by reactor manufacturers, subsequent to the experiments, did show that vendor codes were able to predict the "Semiscale" results with "reasonable accuracy" (17, p. 37).

the experiment was done on something far from a reactor It is difficult to draw conclusions from those experiments What we want to do are more of these experiments (18).

In spite of the limitations of applicability of extrapolations of the experiments, the results were evidently significant enough to spur the investigation being conducted concurrently by Hanauer's task force to climactic activity levels. On June 29, 1971, the task force issued the so-called Interim Acceptance Criteria (see appendix 3). The regulations were considered so urgent that they were put into force without the customary 30 to 60 day comment period. The hastened enforcement precipitated a showdown with environmental intervenors who were already introducing ECCS problems at several licensing hearings. James Schlesinger, then chairman of the AEC, ordered the IAC hearings in an effort to settle the issue once and for all in a single generic rule-making hearing. The importance of the hearings was highlighted by a letter to Schlesinger from the ACRS, dated 10 Feb 1972. It stated in part:

In several previous reports the Advisory Committee on Reactor Safeguards has emphasized the need for high priority for safety research work aimed at gaining a better understanding of the phenomena important to the course of postulated loss-of-coolant accidents (LOCA). In connection with its review of the Interim Acceptance Criteria, the Committee has stated its belief that more work is required on code development, on improved emergency core cooling systems (ECCS), and on safety research oriented to LOCA-ECCS.

The Committee has recently reviewed the general plans of the AEC and the nuclear industry for water reactor safety research. In this review, the Committee had the benefit of a Subcommittee meeting held on December 7-8, 1971, with representatives of the Division of Reactor Development and Technology, the AEC Regulatory Staff, and the nuclear industry.

In this report, the Committee confines its attention primarily to safety research pertinent to LOCA-ECCS. Continuing progress must be made in improving our knowledge in these areas of the increased number of reactors soon to be operating and because some of these reactors are to operate at higher power densities.

After first commenting on the increased ECCS-related research by the vendors, the ACRS letter expressed concern regarding the relative roles of utilities, vendors, and the AEC in this matter. The ACRS proceeded to identify and discuss five areas relating to ECCS performance which needed special emphasis. The five areas are: (1) flow coolant injection; (2) reflooding rates as affected by steam binding; (3) flow and heat during blowdown; (4) improved ECCS computer codes; and (5) fuel-rod failure (62, p. 37).

The hearings were impressive in both quantity of testimony produced and duration. They have produced a number of changes in the criteria (see appendix 3). However, readers may be intimidated by the sheer bulk of the hearings' testimony, as well as the complexity of the technical problems associated with the ECCS. As a result, readers may well be understandably uncertain about whether the results have demonstrated that a "final solution" to the problem has been achieved. The apparent lack of resolution to the problem even within the technical community itself, and the critical needs of decision-makers trying to evolve rational energy strategies for a balanced assessment of the ECCS problem, led EQL to attempt an independent and objective evaluation of the ECCS' problem. This report presents the results of that study.

In essence, the Interim Acceptance Criteria (IAC) developed by a task force headed by Hanauer did two things. First they prescribed a set of general rules, applicable to all reactors, which established design limits for peak fuel temperatures and overall reactor oxidation, as well as requirements that prescribed that core geometry be maintained in a coolable condition during the LOCA transient,* and required that heat removal from the core be assured. Secondly, the IAC specified instructions for utilization of numerical analysis methods for ECCS design and evaluation for each of the principal manufacturers: Westinghouse (W), General Electric (GE), Babcock and Wilcox (B & W), and Combustion Engineering (CE).

A. Criteria for all light-water power reactors

The performance of the emergency core cooling system is judged to be acceptable if the calculated course of the loss-of-coolant accident is limited as follows:

1. The calculated maximum fuel element cladding temperature does not exceed 2300°F. This limit has been chosen on the basis of available data on embrittlement and possible subsequent shattering of the cladding. The results of further detailed experiments could be the basis for future revision of this limit.
2. The amount of fuel element cladding that reacts chemically with water or steam does not exceed 1 percent of the total amount of cladding in the reactor.

* Transient: An event which takes place in a brief period of time.

3. The clad temperature transient is terminated at a time when the core geometry is still amenable to cooling, and before cladding is so embrittled as to fail during or after quenching.

4. The core temperature is reduced and decay heat is removed for an extended period of time, as required by the long-lived radioactivity remaining in the core.

B. Criteria for specific reactors

Each reactor shall be evaluated in accordance with the general criteria above, and using a suitable evaluation model. (Examples of acceptable evaluation models are described.)

The IAC excerpts presented demonstrate the division of the criteria into general and specific elements. Under the general criteria of part A, the first two numbered items were specified in order to protect the reactor against fuel rod cladding embrittlement. Excessive oxidation of the cladding material produces embrittlement as a result of metal-water reactions of the zircaloy with the reactor cooling water, which becomes important at high temperatures, on the order of 2000°F. Limiting the peak temperature experienced by the cladding and the amount of clad oxidation, as specified in the first two criteria, was also apparently designed to limit the additional energy which would be available through the exothermic metal-water reaction to exacerbate the rod temperature excursion, as well as to limit the amount of hydrogen produced in the reaction. Excessive hydrogen production could lead to explosive conditions in the containment vessel. Criticisms (9) of these two criteria were largely directed at the adequacy of simply specifying a maximum fuel rod temperature alone (2300°F), independent of time; the conservatism of the peak temperature limit chosen, if a maximum value alone (without time at temperature criteria) were to be chosen; and the adequacy of the 1 percent overall oxidation limit established

to preclude clad embrittlement or the development of unstable energy input conditions which might exceed the controllability limits of the ECCS.

The third criterion -- preservation of the core geometry in a condition which will permit it to be cooled by the application of the emergency coolant -- was criticized by the Consolidated National Intervenors (CNI) in terms of its inadequacy to even satisfy the definition of a criterion. The principal objection raised was that the language of the criterion was too general to provide adequate direction to a designer to guide his analysis. The CNI called the language "operationally vague" and devoid of sufficient specificity to qualify as an acceptable criterion (9, p. 33).

There has been little debate over the fourth of the listed general criteria. This is because the criterion implies that under applicable conditions the critical thermal transient associated with the LOCA has been controlled and that only long term, relatively low level decay heat removal remains as a problem. Apparently most critics have been willing to accept this aspect of the heat removal problem as being amenable to relatively straightforward engineering solutions.

Specific (but remarkably brief) criteria identifying the "suitable" evaluation models for each reactor manufacturer's designs were given in the IAC (as shown in appendix 3). These models were presented with detail apparently felt to be commensurate with initial AEC concepts of requirements for specification of acceptable assumptions to be made in performance of the ECCS analysis. Specific codes and certain ranges of variables to be used in the analysis were designated in the criteria. The evaluation models were the source of the majority of the criticisms raised by intervenors.

MAJOR PROBLEM AREAS IN LOCA ANALYSIS

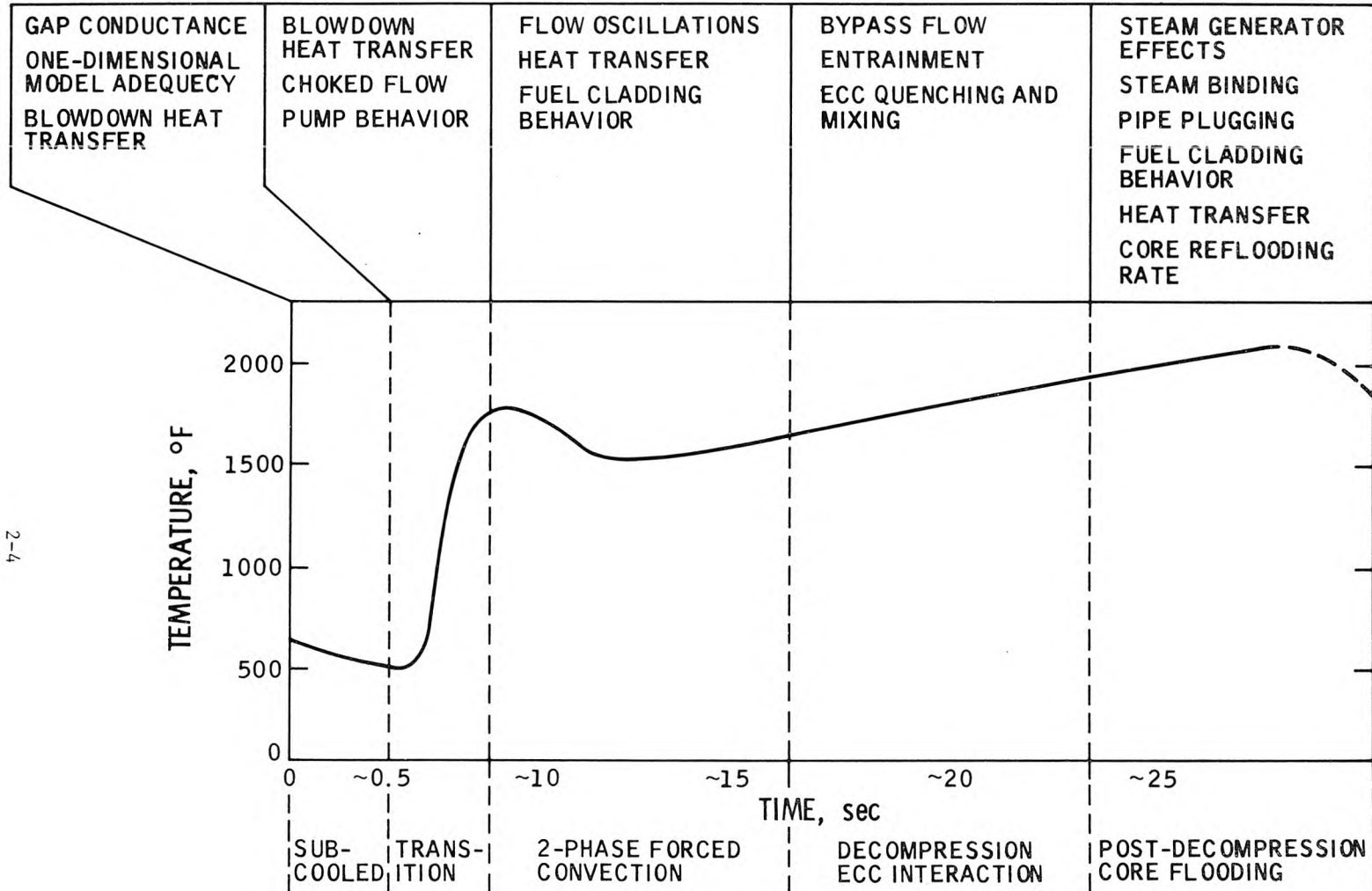


Figure 2.1 PWR LOCA Analysis
Calculated Temperature vs Time Plot
(After Figure 1, 16, by permission.)

2.1 IAC Problem Areas

In general, the criteria were faulted for specification of methods and assumptions whose conservatism could not be adequately defended, in the opinion of the intervenors. To illustrate the magnitude of the unresolved problems which were recognized to be associated with the ECCS (at approximately the time the IAC were published), figure 2.1 has been abstracted from the AEC's Water Reactor Safety Program Augmentation Plan (16).

In figure 2.1, the major problem areas of LOCA/ECCS analysis were identified by the AEC as they occur chronologically in a LOCA in a pressurized water reactor (PWR). The figure indicates, as a function of the various distinguishing periods of the accident, the specific areas within each regime where needed improvements were recognized and called for in methods of analysis. The temperature history shown is only a schematic representation of a LOCA thermal excursion. However, the general pattern shown of the temperature-time-thermodynamic process scenario provides a basic description of LOCA events in a PWR.

Detailed discussions of the specific problem areas, as they are related to the various thermodynamic regimes shown in figure 2.1, are given in subsequent portions of the report. However, a "general observation" given by the AEC in connection with this figure should be included here.

...As was stated previously, the end product of the safety program must generally result in the development of improved analysis methods. To date, the evolution of codes has not kept pace with the development of ECCS systems.

As reactor designs and their operating characteristics changed, the analysis methods were "patched up," rather than redeveloped, with the net result that, overall, existing methods are inefficient, inflexible, and do not adequately represent the physical phenomena intended (16).

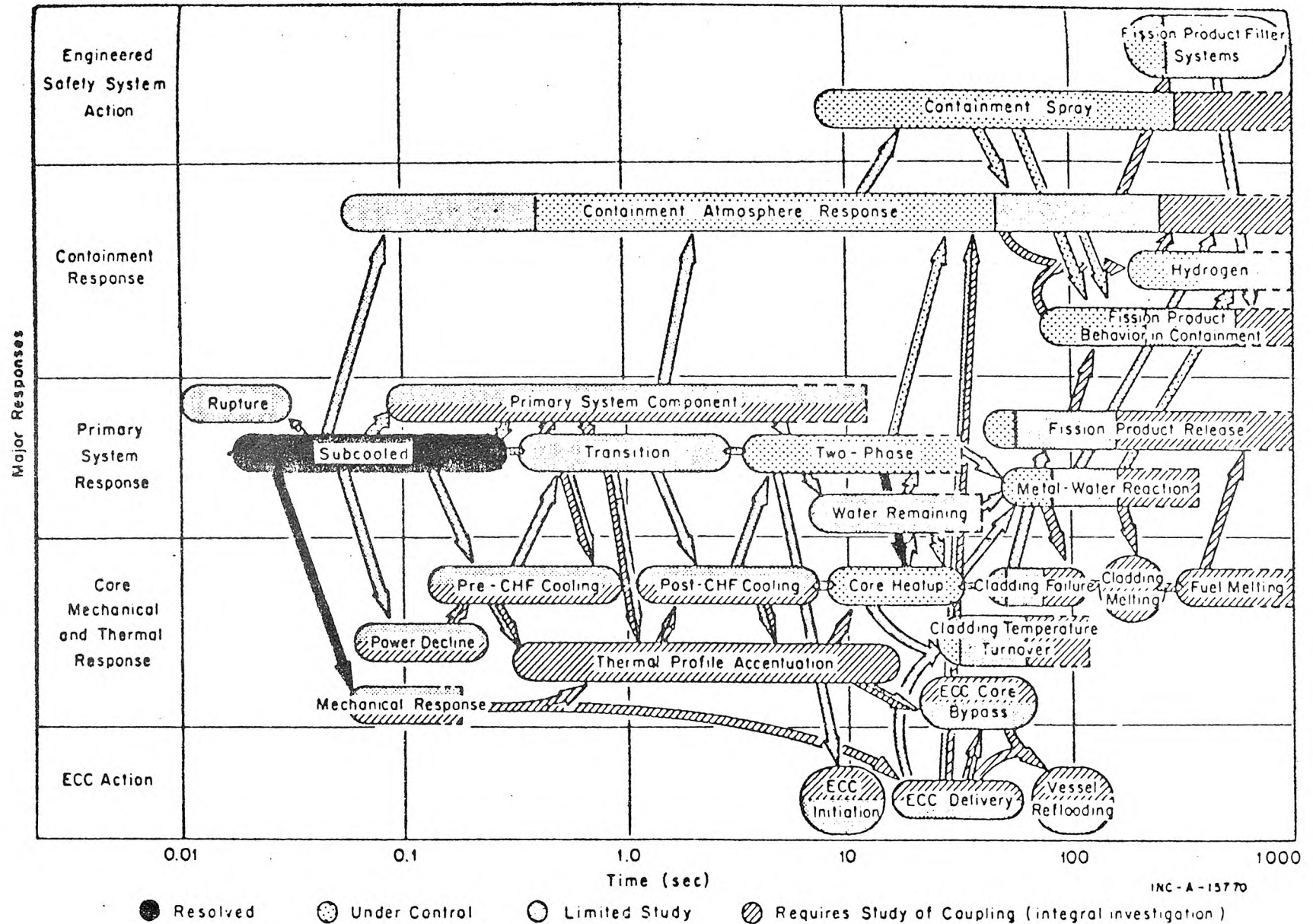
To further illustrate the problem and indicate the extent of the disputed areas of uncertainty in analysis of thermodynamic response, figure 2.2 has been reproduced (9, p. 1.13A) from a document of the Aerojet Nuclear Corporation (ANC), the operators of the National Reactor Testing Station (NRTS) facilities of the AEC at Idaho Falls. Again, as in figure 2.1, the various events of the LOCA are shown in the framework of a time history of the response of major reactor system elements. The numerous problem areas for which incomplete understanding existed at the time are clearly shown in this figure.

The problems which will be addressed in most detail in this report dealing with the unknowns of ECCS are concerned primarily with evaluation of system responses of the lower half of the figure: the primary system response, the core mechanical and thermal response, and the ECC action. Although the problems of containment response and the engineered safety system are not unimportant to reactor safety, most are felt to be relatively well understood. It is mostly in connection with their interaction with other elements of the system that responses are uncertain. Within this framework, some reference to their effects will be made in subsequent discussion.

These problems have been a subject of continuing review on the part of the Advisory Committee on Reactor Safeguards (ACRS). The United States Congress, in setting up the AEC, established the ACRS. The Committee is composed of experts in various aspects of reactor safety whose function is to advise the Commission with respect to "hazards of proposed or existing reactor facilities and the adequacy of proposed reactor safety standards" along with other responsibilities.

In response to direct questions submitted to the ACRS by the intervenors in the ECCS hearings, a written statement of reply was formulated by the Committee related to uncertainties in analysis methods utilized in ECCS evaluation. The ACRS stated:

Figure 2.2
ANC Outline of LOCA Problem Areas



(After Figure 15, 9, p. 1.13A, by permission)

The ACRS takes the view that items in the approved evaluation models are proven to be conservative when fully confirmed by experimental evidence and supporting analytical studies. On this basis, the following representative items are considered not proven to be conservative, and are undergoing investigation:

- Analytical models and numerical methods.
- Amount of initial stored energy in the fuel.
- Treatment of phase separation and nonequilibrium effects.
- Treatment of loop resistances.
- Treatment of hot channel flow, including flow blockage and flow redistribution.
- Treatment of break flows.
- Treatment of decay heat.
- Transient critical heat flux and heat transfer.
- Treatment of clad ductility.
- PWRs -- Distribution of injected water -- Reflooding rates, reflood heat transfer, and carryover.
- BWRs* -- Level swell -- Spray heat transfer.

While the above aspects are considered not proven to be conservative, the ACRS nevertheless believes that they can be handled in such a manner that there is reasonable assurance that, with appropriate use of the Interim Acceptance Criteria and other applicable design and evaluation criteria, water reactors of current design can be operated without undue risk to the health and safety of the public (19).

The fifteen items of uncertain conservatism specified by the ACRS provide a succinct list of the problem areas associated with the IAC which were addressed in the hearings. A brief evaluation of some of the critical items for which the ACRS felt that conservatism had not been adequately established is presented in the following chapter

* BWR: Boiling Water Reactor (see appendix 1).

of this report. Detailed analyses of the items reviewed are given in appendixes 5 to 10.

The final Acceptance Criteria (AC) issued by the AEC on December 28, 1973, contains a number of substantial changes, when compared to the original IAC. The complete text of the AC is presented in appendix 3, along with the text of the IAC. A detailed discussion of the important changes in the AC and their implications is presented in the subsequent chapters.

3.0

SUMMARY AND EVALUATION OF HEARINGS RESULTS

Although the vast [IAC Hearing] record thus far developed in this proceeding has been marred by excessive focus on peripheral matters and seemingly interminable arguments among counsel and between counsel and the Board in an atmosphere too closely 'akin to a criminal trial portrayed in the popular media', there is nevertheless, a substantial amount of testimonial and documentary evidence on the central technical issues in the proceeding. Each of the principal participants, including the staff, presented evidence in support of its views. In addition, the staff presented for the evidentiary record certain divergent technical viewpoints on particular technical subject areas. The open inquiry directed by the Commission in the rulemaking proceeding has adduced evidence from the various participants -- of diverse qualitative weight -- which is designed to support points of view running across the entire decision spectrum (from support for a peak clad temperature of 2700° at one end to a call for a virtual moratorium on power-reactor licensing at the other). Nevertheless, it is the staff's view that a critical evaluation of all of the evidence in the record of this proceeding as it has developed thus far will show that the reliable, probative and substantial evidence provides full and firm support for the improvements to the Interim Criteria proposed by the staff.

AEC Concluding Statement
(6, p. 20)

This quote acknowledges the AEC's recognition of the bulk and complexity of the IAC Hearing Record. Evaluations of the hearings' results by the various participants were as diverse as the evidence presented in the hearings' record, ranging from claims of excessive conservatism to non-conservatism. This chapter will present an overview of the hearings. The hearings' results are reviewed in terms of the adequacy of the IAC, changes to the IAC which were recommended in the staff's "Concluding Statement" with its Proposed Rule (PR) and finally the ultimate changes made in the AC, with specific (but concise) problem analyses (presented in greater detail in the appendices). A general evaluation of the criticality of ECCS parameters will also be presented.

3.1 ECCS Hearings Results

The results of the hearings must be evaluated around two questions. The first question is: What did the hearings contribute to the evaluation of the adequacy of the IAC? The second question is: What changes occurred in the ECCS' criteria as a result of the hearings and how did they affect the criteria conservatism?

3.1.1 Adequacy of the IAC

The question of evidence for the adequacy of the IAC was indirectly answered in the testimony of Rosen and Colmar. Dr. Morris Rosen and Mr. Robert Colmar were two members of the AEC regulatory staff who made outspoken criticisms of the IAC. Dr. Rosen was the Technical Advisor to the Director of Reactor Licensing and headed the branch of the staff that served as a focal point for ECCS performance evaluations from 1967 until January 1972. Colmar, a senior nuclear engineer on the regulatory staff under Rosen, was the principal investigator assigned by the ECCS Task Force to study flow blockage and its effects. The two men were dissenting members of the regulatory staff and presented testimony concerning their objections at the hearings. At one time during their testimony, Board Member John H. Buck questioned them about the differences between their observations and evaluations of data and those of the staff panel. Buck drew from Rosen and Colmar the acknowledgement that both they and the panel had been working with the same data and the same consultants. As reported in a Nuclear Industry editorial, Buck observed:

But it seems to us, as may be natural in a situation like this, there is some difference in philosophy and some differences in judgment that results from that data and from the people to whom you listen and perhaps the philosophies that you have as to where you want to go.

When the two [Rosen and Colmar] indicated they still feel insufficient experimental information exists on which to develop adequately the Interim Acceptance Criteria,

Buck asked them if they feel the regulatory staff thinks it has enough experimental data. Rosen replied:

'I would have to say that if the results of this hearing lead to a reevaluation of the...criteria, I would have to assume that somehow the staff felt they did not have sufficient information to make their earlier recommendations or that new information has been produced in the last several months to make them reevaluate' (20, p. 33).

With these words, Rosen provided a reasonable means for evaluating the adequacy of the IAC. If changes were made, then the material presented at the hearings, or produced during them, must have illuminated inadequacies in the original criteria.

Though changes did occur between formulation of the IAC and the AC, the staff panel members never conceded that the IAC were not entirely adequate. Near the conclusion of the hearings, Dr. Stephen Hanauer, the staff panel chairman, was cross-examined by Ford of CNI with respect to his evaluation of the IAC. The testimony is recorded as:

- Q. [by Mr. Ford]: In retrospect, Dr. Hanauer, is it your present view that the criteria [and evaluation models] promulgated in June of 1971 are not overall suitably conservative, but there is in fact a need for additional conservatism in several cases?...
- A. [Dr. Hanauer]: Well it is certainly true that the Regulatory Staff has recommended that certain changes be made in the criteria [and models] which make them more conservative...That does not make them necessarily unsuitable. The target is moving, and the information being obtained changes some of our information from time to time.
- Q. [by Mr. Cherry]: Now, Dr. Hanauer,....[d]o you still believe that the [original] evaluation models are suitably conservative over-all in effect...?
- A. [Dr. Hanauer]:...Well, frankly, I don't know the answer today. It's not a question I have addressed myself to. I have gone past that and recommended some changes...I suspect, though I have not considered the question in some time, that the old, unimproved

evaluation models might still be found by me to be found suitably conservative although I have proposed changes (Tr. 19792-94)** (22, p. 19-20) (emphasis added).

In spite of possible claims for IAC conservatism, the staff did recognize the PR changes as improvements. In the words of their "Concluding Statement":

The proposed new regulations are believed by the staff to constitute an improvement over the Interim Policy Statement. The only significant change in the acceptance criteria themselves is the replacement of a single temperature limit by a combination of temperature and oxidation limits - a change foreseen in the Interim Policy Statement itself. The changes in the evaluation models require various aspects of the calculation to be done better, define better the procedures and parameters used, and take better account of the various physical phenomena now known to occur during postulated LOCA's (6).

Though the significance of PR changes were minimized by the staff, the changes were extensive and substantial. In the environmental impact statement prepared in connection with the PR changes, the costs of the changes were acknowledged, as follows:

As will be seen in the following sections, the costs of the Proposed Rule, with its conservatisms, are not unsubstantial. In the Staff's view, however, conservatisms are warranted for the protection of health and safety given the present state of knowledge, and it does not appear to the staff that the attendant costs are in imbalance with the desired safety objective (61, p. 99) (emphasis added).

In the sense of Rosen's testimony, that changes made in the IAC implied staff recognition of the validity of pertinent criticisms, the Proposed Rule (21) did itself make a statement about IAC adequacy, which will be discussed in more detail in the remainder of this chapter.

Generally speaking, the new AC incorporated most of the recommendations of the PR. However, there are some differences of substance between the sets of specification. No serious attempt will be made to detail the differences between AC and PR. Principal emphasis

** References to pages in the official Hearings Transcript.

will be placed upon evaluation of the AC and comparison of its changes with respect to the IAC.

3.1.2 The Acceptance Criteria - revisions to the IAC

In brief, the AEC has identified the principal changes in the AC, as compared with the IAC, in the following words:

The Interim Policy Statement includes: (1) general criteria for emergency core cooling systems applicable to all light-water power reactors (the Interim Acceptance Criteria, or IAC), (2) requirements for analysis using a suitable evaluation model, (3) provisions for application to various classes of reactors by specified dates, (4) provision for variance under stated conditions, and (5) a listing of acceptable evaluation models. The new regulation has sections serving the same purpose as (1), (2), (3), and (4) above. No complete listings of acceptable evaluation models accompany this decision. The required and acceptable features of evaluation models, however, will provide the basis for the Regulatory Staff to determine the acceptability of such models as may be furnished.

The principal changes from the Interim Policy Statement are as follows. The old criterion number one, specifying that the temperature of the zircaloy cladding should not exceed 2300°F is replaced by two criteria, lowering the allowed peak zircaloy temperature to 2200°F and providing a limit on the maximum allowed local oxidation. The other three criteria of the IAC are retained, with some modification of the wording. These three criteria limit the hydrogen generation from metal-water reactions, require maintenance of a coolable core geometry, and provide for long-term cooling of the quenched core.

The most important effect of the changes in the required features of the evaluation models is that swelling and bursting of the cladding must now be taken into consideration when they are calculated to occur, and that the maximum temperature and oxidation criteria must be applied to the region of clad swelling or bursting when the maximum temperature and oxidation are calculated to occur there. Another important change is the requirement that, in the steady state operation just before the accident, the thermal conductance of the gap between the fuel pellets and the cladding should be calculated taking into consideration any increase in gap dimensions resulting from such phenomena as fuel densification and should also consider the effects of the presence of fission gases.

When these effects are taken into consideration a higher stored energy may be calculated. Other changes in the evaluation models are mostly in the direction of replacing previous broad conservative assumptions with more detailed calculations where new experimental information is available or where better calculational methods have been developed.

The wording of the definition of a loss-of-coolant accident has been modified to conform to its long-accepted usage, limiting it to breaks in pipes. Justification for the exclusion of consideration of pressure vessel failures from the LOCA is extensively discussed throughout Volume 39 of the transcript (April 11, 1972), and we have referred to it earlier (pp. 6-8).

The new regulations also require a more complete documentation of the evaluation models that are used (60, p. 1093) (emphasis added).

In essence, the AC is much more complete in detail and encompasses many more specific problem areas than the IAC. In particular, its treatment of the problems cited in the hearings is a great deal more detailed than in the IAC. The sections of the AC seem to address themselves very closely to the items listed by the ACRS as being of uncertain conservatism (listed in sec. 2.1).

3.2 Analysis of IAC Problem Areas Within the Context of the New Acceptance Criteria

This section summarizes material (presented in detail in several appendices) analyzing the ACRS' list of items "considered not proven to be conservative" in context with the changes implied in the new Acceptance Criteria.

3.2.1 Metal-Water reactions, energy release and rod embrittlement

In the areas of energy release and rod embrittlement from metal-water reactions, the AC shows the most evident changes. The reduction in the peak cladding temperature criterion (from 2300°F to 2200°F) and the imposition of a new local oxidation limit for the cladding (17 percent equivalent conversion to ZrO_2 based upon the Baker-Just oxidation model) have been described as the most significant changes in the AC.

In their Opinion Statement for the new Acceptance Criteria the AEC Commissioners stated:

It should be remembered that the calculations that are made of the effectiveness of the ECCS center on maintaining the integrity of the zircaloy cladding, since if it remains intact we can be sure that the uranium dioxide fuel pellets will be kept separate and coolable. To keep the zircaloy intact requires controlling its maximum temperature and oxidation (60, p. 1091),

and

Our selection of the 2200°F limit results primarily from our belief that retention of ductility in the zircaloy is the best guarantee of its remaining intact during the hypothetical LOCA (60, p. 1098).

In addition, the AC requires the inclusion of oxidation on the inside of swollen and ruptured cladding as well as the outside, a feature neglected in the IAC. The AC requires that calculation of the internal rod oxidation begin at the calculated time of rupture, with the use of Baker-Just oxidation model required in the calculation. Moreover, the calculated 17 percent limit must include in a specified manner both internal and external oxidation, as described above, as well as the thinning of the clad during the swelling and rupture process.

Though the new criteria are greatly improved over the elemental 2300°F temperature limit of the IAC, in the opinion of the author they do not appear to have achieved assured conservatism in all respects.

As discussed in appendix 7, the AEC has consistently deemphasized the potential importance of the energy release rates from the zirconium-steam reaction. The AEC Concluding Statement dismisses the energy release problem with a single sentence. It states:

Melting and energy release from zirconium-steam reaction are not the basis for specifying a 2200°F limit; in fact, a 2300°F limit would be sufficient in this regard (6, p. 75).

Adequate evidence that energy release rates can be dismissed in this manner has not been (and in fact cannot be) presented. In appendix 7, figure A7.3 and the accompanying explanation show that at 2200°F the 17 percent equivalent ZrO_2 oxidation limit does not proscribe the allowable time duration for exposure to the 2200°F peak temperatures sufficiently to preclude energy release rates which may be of the same order of magnitude as, or greater than, the decay heat release. The Commission has given more adequate recognition to this energy source in its opinion to the AC where it stated:

In addition to the primary heat transfer effects of taking into consideration the swelling and rupture of the cladding, there would be important secondary effects arising from the steam oxidation of the cladding by the steam. Higher temperatures would lead to increased oxidation, which would contribute to a further increase in temperature, and the opening in the cladding would allow oxidation on the inside, again increasing the calculated temperature (60, p. 1106).

Though the potential importance of the energy release from the zirconium-steam reaction has been poorly acknowledged by the AEC staff, the method for evaluating it has been conservatively prescribed in all of the criteria documentation. The required use of the Baker-Just oxidation model gives conservative estimates of metal-water reaction energy release for temperatures above 1900°F. When combined with the AC requirement that the "reaction shall be assumed not to be steam limited" (60, p. 1134), the energy release estimates should be adequately conservative.

From the standpoint of limiting rod embrittlement, the 17 percent equivalent ZrO_2 oxidation criterion is of borderline conservatism. As discussed in connection with figure A7.6 (appendix 7), this amount of oxidation would probably put the rod in a "partially ductile" condition, for which brittle failure in the course of the LOCA could not be positively discounted. At the 17 percent oxidation limit, the rod is left with a zero ductility temperature (ZDT) of approximately 900°F, below

which the rod has no ductility and the probability of brittle failure is increased. A ZDT of 900°F is uncomfortably high, since maximum LOCA quenching stresses are induced as rod wetting occurs at temperatures from approximately 700 to 1000°F (25, p. 3-15). It appears that the Combustion Engineering recommendation for embrittlement oxidation limits of $F_w > .65$ (see table A7-2, appendix 7), which corresponds to an equivalent ZrO_2 oxidation of about 10 percent to 14 percent and a ZDT of approximately 400°F, would be a more acceptably conservative limit. The Consolidated Utilities Group has also recognized the conservatism of a lower oxidation limit. They have stated:

....a limit on the calculated....equivalent oxidation of 12 mole percent would prevent clad embrittlement and failure and should conservatively bound conditions which could be experienced during a design basis LOCA (22, p. 39).

In conclusion, the AC metal-water reaction criteria limits of 2200°F and 17 percent equivalent ZrO_2 oxidation, though improved over the IAC criterion, are still of borderline conservatism. The combined criteria do not eliminate the potential for metal-water reaction energy release rates of the same order of magnitude as those from decay heat nor do they preclude excessive embrittlement. Lower equivalent oxidation limits (12-14 percent) would give increased conservatism. However, the use of the Baker-Just oxidation model, especially with the AC's exclusion of the assumption of steam limitation for the reaction, does give conservative estimates of total oxidation and energy release. Consequently, when used in the evaluation models, the current oxidation rate relations are apparently suitably conservative in prescribing the magnitude of the oxidation produced and energy release, though the 17 percent oxidation limit does not guarantee prevention of brittle rod failure. Assuming nominal power peaking factors (local power distribution) within the core, rods which might reach the 2200°F criteria limit are likely to be localized to a relatively small region of the core -- from 5 to 15 percent

of the total area of the core. If clad exposure is limited to criteria levels of less than 17 percent equivalent zircaloy oxidation, then massive melting and core geometry changes resulting from brittle failure of fuel rods appears unlikely. Fuel elements are likely to be relatively cool at the time brittle failure might occur (on the order of 1000°F), and relatively small amounts of material would be likely to be dispersed when cladding failure occurred. Consequently, additional mechanical damage resulting from melting in the core induced by brittle failures of the fuel rods should be relatively light and coolability would probably not be seriously perturbed. Thus, the uncertain conservatism of the 17 percent oxidation limit on prevention of brittle failure of the fuel rods has probably a second-order effect on large scale distortion and/or melting of the core. Under these circumstances, the consequences of possible brittle failures of the fuel rods are not expected to be of major significance in the LOCA sequence of events, even if such failures should occur.

3.2.2 Initial stored fuel energy and related thermal parameters

Information pertinent to initial stored fuel energy is found in sections IAl and IB of appendix K of the AC (cf appendix 3), reproduced below.

Section IAl.

The Initial Stored Energy in the Fuel. The steady-state temperature distribution and stored energy in the fuel before the hypothetical accident shall be calculated for the burn-up that yields the highest calculated cladding temperature (or, optionally, the highest calculated stored energy). To accomplish this, the thermal conductivity of the UO_2 shall be evaluated as a function of burn-up and temperature, taking into consideration differences in initial density, and the thermal conductance of the gap between the UO_2 and the cladding shall be evaluated as a function of the burn-up, taking into consideration fuel densification and expansion, the composition and pressure of the gases within the fuel rod, the initial cold gap dimension with its tolerances, and cladding creep.

Section IB.

Swelling and Rupture of the Cladding and Fuel Rod
Thermal Parameters

Each evaluation model shall include a provision for predicting cladding swelling and rupture from consideration of the axial temperature distribution of the cladding and from the difference in pressure between the inside and outside of the cladding, both as functions of time. To be acceptable the swelling and rupture calculations shall be based on applicable data in such a way that the degree of swelling and incidence of rupture are not underestimated. The degree of swelling and rupture shall be taken into account in calculations of gap conductance, cladding oxidation and embrittlement, and hydrogen generation.

The calculations of fuel and cladding temperatures as a function of time shall use values for gap conductance and other thermal parameters as functions of temperature and other applicable time-dependent variables. The gap conductance shall be varied in accordance with changes in gap dimensions and any other applicable variables (from appendix 3).

The PR prescribed that steady state gap coefficients be evaluated on a case-by-case basis for each vendor's reactor designs, since "substantial differences" exist in the fuel cladding designs between vendors and even with a given vendor's product lines. In formulating the AC, the Commission retained this case-by-case analysis requirement (60, p. 1101).

The IAC treatment of initial stored fuel energy was notable for its brevity. It stated, for example, "peak cladding temperature has been shown to be relatively insensitive to changes in gap conductance during an accident" (8, p. 4-26). Relatively large values of gap conductance were prescribed (1000-2400 B/hr-ft²-°F), apparently without substantial evaluation, as they had been recommended by the vendors. With such relatively high gap conductances, fuel rod heat transfer processes were dominated by the low convective film heat transfer coefficients

between rod and coolant (generally less than 100 B/hr-ft^2) during blow-down and reflood, so that the significance of the gap conductance for heat transfer of the initial stored energy was minimized. (This is discussed more fully in appendix 6.)

The AEC appeared to progressively reevaluate the importance of gap conductance and initial stored fuel energy as the hearings proceeded. The AEC Supplemental Testimony reported the results of a parametric investigation of the influence of gap conductance on initial stored fuel energy release during the LOCA (4, pp. 10-16 to 10-23) (also appendix 10). In this investigation, the important effect of clad swelling and rupture during the LOCA on gap conductance with its consequent impact on the transfer of stored fuel energy was demonstrated. The study showed that substantially lower gap conductance values were calculated (between limits of approximately $5\text{-}100 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$) than were prescribed in the IAC ($1000\text{-}2400 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$). At low values such as these, frequently less than concurrent convective film heat transfer coefficients, gap conductance became very important. It was observed that when swelling occurred during blowdown, a reduction in gap conductance from steady state values of about $800\text{-}1000 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$ to approximately $5\text{-}100 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$ resulted in frequent corresponding increases in peak temperatures as high as 100°F to 200°F , and sometimes resulted in uncoolable conditions. (Additional observations on this AEC parametric study are presented in sec. 3.3.1 and appendix 10.)

Between the publication of the Supplemental and Concluding Statements, the AEC moved to correct the IAC deficiencies with respect to the stored fuel energy. The Commission Opinion in presenting the AC strengthens and clarifies the PR recommendations. The AC now provides criteria for initial stored fuel energy which approach "assured" conservatism. The influence of clad swelling and rupture on the "hot rod" calculations for DBA and the requirement to select parameters influencing the initial stored fuel energy so that it is maximized are definite steps in the direction of assured conservatism.

If there are faults with the AC, they are: (1) There are no quantitative specifications (or limits) given for important thermal parameters and (2) case-by-case evaluation of gap conductance is required. Though the effect of (2) would appear to at least partially compensate for the potential problems with (1), the AC approach implies some continuing uncertainty on the part of the AEC over how to deal with the initial stored fuel energy.

3.2.3 Fission product decay heat

Though the AC goes into more detail about required assumptions of reactor operating power levels and in-core power distribution (peaking factors) prior to LOCA and presents a more complete description of how reactor kinetics at shutdown must be handled, the basic IAC criterion for fission product decay heat (i.e., utilization of the proposed ANS 5.1 Standard + 20 percent) has been retained without modification.

During the early stages of the hearings, a substantial controversy was provoked by the CNI over the adequacy of the proposed ANS 5.1 Standard to correctly predict the decay heat rate in the critical LOCA time period of approximately 1000 sec after break initiation. The technical basis for questioning the ANS 5.1 Standard + 20 percent criterion (see appendix 5) centered around recent decay heat investigations of England (23). When the CNI first introduced England's analysis into the hearing record, serious questions were raised about the adequacy of predictions of the magnitudes of early decay heat release rates and the asymptotic decay heat limit prescribed by ANS Standard 5.1 for fuel exposed to irradiation for large integrated flux-time values. England's doctoral dissertation developed a method for improving on existing neutron capture analysis techniques through incorporation of a more complete physical model of the coupling of short half-life nuclides in the fission product chain. England's dissertation results gave an indication that fuel exposed to large neutron flux-time histories would experience significant increases (as much as a factor of 2 or more) in

decay heat release at shutdown. Following publication, England's initial thesis (23) analysis was found to have errors in numerical programming and input data. Although a corrected study (24) showed agreement in principle with the original thesis (note appendix 5, figure A5.3), the magnitude of predicted increases was significantly reduced to less than 10 percent for a relatively low flux-time irradiation history case.

Comparison of results of England's code (CINDER) with other sophisticated numerical summation analysis methods (when appropriate corrections for coding and input data errors have been made) shows the need for an upward correction to the ANS Standard 5.1 of approximately 6 percent for the low flux-time irradiation cases. When appropriate flux levels and irradiation periods are considered for the AC requirements of "hot rod" calculations, a need for a total net upward correction to the ANS Standard 5.1 of about 10-15 percent is required, at shutdown times of approximately 1000 sec.

When empirical results are considered as a basis for evaluation of summation calculations, we observe that there is good general agreement between CINDER calculations and the empirical data for shutdown times greater than 100 seconds. The "best estimates" of such experimental data (65) give direct quantitative support to an upward deviation from the ANS Standard 5.1 of 6 percent or better at shutdown time of the order of 1000 seconds. Moreover, Perry et al. have estimated a one-standard deviation uncertainty of the order of ± 15 percent in the empirical results.

Therefore, it appears that in the critical LOCA shutdown period of about 1000 seconds ANS Standard 5.1 ± 20 percent will correspond to an equivalent deviation uncertainty of about one standard deviation from the expected mean decay heat values. It is legitimate to ask whether confidence limits are adequately and conservatively bounded at one standard deviation. ANS Standard 5.1 ± 20 percent appears to be equivalent to a one standard deviation uncertainty, and the probability that

higher decay energies will be experienced above the AC prescribed limits is above 30 percent. Increasing the decay energy limits to ANS Standard 5.1 + 30 percent would reduce the probability of experiencing decay heats above the criteria limits to about 11 percent, but if the criteria limits were raised to ANS Standard 5.1 + 35 percent, the probability is less than 5 percent that the criteria limits would be exceeded. A 30 percent probability of exceeding the AC (ANS Standard 5.1 \pm 20 percent) limits seems inadequately conservative. Consequently, it would seem desirable to increase the bounding criteria values to either ANS Standard 5.1 + 30 percent, or ANS 5.1 + 35 percent.

3.2.4 Break flows

Break flows are a major determinant of the blowdown rate and duration. Consequently, their potential impact on the thermal history of the reactor is evident. Break flows greater than anticipated could induce shorter blowdown periods with potentially increased containment pressures and probable increases in fuel rod temperatures prior to emergency coolant injection.

The AEC has prescribed the use of the Moody fluid discharge model (55) both in their Concluding Statement (6, p. 105) and the final AC (60, p. 1108) as well as in their initial Direct Testimony (8, pp. 2-41 & 4-15), and in the IAC themselves. Moody's analysis method was developed for flow of a two-phase (liquid-steam) mixture through pipes based upon an idealized isentropic equilibrium model of the flow. CNL attempted to show that for relatively short pipes, where the length-to-diameter ratio was short (less than 10), two-phase equilibrium would not exist and metastable liquid flow (flow of pure liquid at temperatures and pressures where vaporization would be expected under equilibrium conditions) would take place (9, chapter 8). Based upon experimental results of Fauske (56), under these conditions greatly increased break flow rates could be obtained ("1.7 times greater than the rate predicted by the designers for two-phase flow")(9, p. 8.1).

Examination of experimental results, including Fauske's (appendix 9, figure A9.1) shows that significant differences exist between initial metastable flow (in pipes of very short length and small diameters) and equilibrium flow rates.

The highest metastable flow rates are approximately a factor of three greater than the equilibrium choked-flow conditions for relatively long pipes. It is apparently this potential for flow rates truly substantially different than those predicted for equilibrium flow that caused concern of the ACRS and CNI.

The reactor manufacturers have challenged the validity of a break flow model based upon metastability concepts. They have suggested, alternatively, that metastable flow is a function of pipe length only. Consequently, it would only be observed for very short distances (of less than one foot) compared to typical break lengths of approximately three feet or more.

The intervenors have argued that Fauske, and the majority of the experimental evidence, supports the concept that metastability is better related to the ratio of break length to pipe diameter than to break length alone. For the large pipes of interest in reactors, frequently as large as two feet in diameter, the length-to-diameter ratios are well within the critical values associated with experimentally observed metastable flow for the small pipes (for which experimental data exists) (figure A9.2, appendix 9).

Examination of the experimental data appears to support the intervenor claim that break flows, as predicted by the Moody model, could be substantially underestimated. The regulatory staff tried to overcome this problem in their Concluding Statement by calling for more than one model of blowdown break flow to be used in analyzing critical flow in accordance with the revised criteria of their Proposed Rule (6, pp. 105-108). Thus the staff concluded that,

The critical flow model of Moody is appropriate for use in break spectrum analyses of blowdown transients in BWRs and PWRs on the basis that it overpredicts blowdown flow...whenever the break exit plane quality is greater than about two percent...However, for the blowdown period during which subcooled liquid, saturated liquid or low quality two-phase fluid exists at the break exit plane, the Moody model underpredicts experimental discharge data... Therefore, the Proposed Rule requires the use of a model which is more appropriate to these fluid conditions. One such model contained in the evidence of this proceeding is the modified Zaloudek model of Westinghouse (Exhibit 1151, [57] Section III). The Moody model may also be applicable for early times during blowdown before the exit plane quality reaches two percent if it is used with a Moody multiplier of greater than unity...The staff concludes on the basis of this evidence that models appropriate to these flow regimes do exist (6, p. 108) (emphasis added).

In its revised AC, the Commission has downgraded the importance of the early blowdown stages where the need for a Moody multiplier of greater than unity had been recognized. The AC now require that,

For all times after the discharging fluid has been calculated to be two-phase in composition, the discharge rate shall be calculated by use of the Moody model----the calculation shall be conducted with at least three values of a discharge coefficient applied to the postulated break area, these values spanning the range from 0.6 to 1.0 (60, p. 1108).

Though the Commission acknowledged that recommendations for discharge coefficients greater than one were widespread, their discussion of the AC implies that the Moody model is always conservative and that the metastable flow period (if any) is adequately covered by Moody model results. They acknowledge that

There was widespread agreement that a variable discharge coefficient provides a better fit to the data than a constant one....Ybarrando (Transcript p. 6362) reported the result of an ANC calculation with a discharge coefficient initially 2.0 and later in blowdown 0.6, where the first peak in the clad temperature exceeded by about 100°F the value obtained with a fixed discharge coefficient of 1.0 (60, pp. 1111, 1112) (emphasis added).

Nevertheless, the Commission concluded

We agree with the Staff position as to correctness of use of the model based on critical flow, since the length of time available during the blowdown far exceeds the amount needed for nucleation and build-up of two-phase discharge. Furthermore, the evidence is strong that use of the Moody correlation does not underestimate observed experimental discharge rates, as would be the case if discharge were really metastable, but in fact it definitely overestimates the discharge rates (60, p. 1112).

The question of the adequacy of the experimental data was not addressed in any significant manner in the AC discussion. However, it should be noted that little or no experimental data exists for realistic break flow from large pipes.

In summary, it appears that the Moody break flow model may underpredict nonequilibrium metastable flow by nearly a factor of two (at least for a brief period), as indicated by the CNI. To account for this possibility, the regulatory staff suggested in the PR that a different model, although not entirely explicitly specified, be used for estimating flow rates at the beginning of the blowdown period. Use of this model would have predicted flow rates greater than those estimated with the Moody model. In the final AC, the Commission eliminated the staff recommendation, concluding that the Moody model (with discharge coefficients less than or equal to one) was always conservative.

In view of the limited experimental basis for models which may be applied to the large diameter pipes associated with a DBA for large operational reactors, it would appear desirable to perform additional break flow tests with more representatively sized equipment. In the absence of experimental data which clearly supports the Moody model or, alternatively, demonstrates the validity of a nonequilibrium model of break flow metastability, more conservative and definitive specifications should be given in the criteria. Specifically quantified values of Moody

multipliers (greater than one) should be prescribed, along with their period of application, in order to assure that conservatism is attained for break flow specifications.

3.2.5 Transient critical heat flux and blowdown heat transfer

At the beginning of the LOCA transient, heat is transferred from the fuel rods to the coolant water in a continuation of the highly efficient nucleate boiling heat transfer mode of normal (steady state) reactor operation. Heat transfer coefficients under nucleate boiling conditions are immensely higher (approximately a factor of 10,000) than those which occur during the core spray or initial reflood portions of the cooling process. During the rapid system decompression accompanying blowdown, a transition in the boiling process takes place from pinpoint nucleate boiling to film boiling and two-phase (liquid-vapor) convective cooling, which greatly reduces the system cooling capability.

In describing the LOCA transient, analysis methods depend upon the development of the concept of time periods during which a given boiling transitional condition occurs (e.g., time to critical heat flux, time to departure from nucleate boiling, or duration of the period of stable film boiling). The blowdown process induces the first important departure from nucleate boiling. This transition achieves its importance because the rapid rod cladding dryout accompanying blowdown makes re-establishment of the nucleate boiling, high heat transfer conditions for the rod very difficult. Moreover, the conditions required for such re-establishment (rewetting) are uncertain. As a result of this uncertainty, the revised criterion conservatively requires that rewetting be neglected during blowdown, immediately after critical heat flux (CHF) is first predicted. The new AC state:

After CHF is first predicted at an axial fuel rod location during blowdown, the calculation shall not use nucleate boiling heat transfer correlations at that location subsequently during the blowdown even if the calculated local fluid and

surface conditions would apparently justify the reestablishment of nucleate boiling. Heat transfer assumptions characteristic of return to nucleate boiling (rewetting) shall be permitted when justified by the calculated local fluid and surface conditions during the reflood portion of a LOCA (60, p. 1109) (emphasis added).

Neglect of rewetting during blowdown, after departure from nucleate boiling (DNB), was apparently also practiced under the IAC and precludes the assumption of redevelopment of nucleate boiling conditions.

The importance of the heat transfer occurring after CHF or DNB has been recognized by all those who have investigated the LOCA. In its discussion of the AC, the Commission stated:

The rate at which heat is transferred from the clad to the water after departure from nucleate boiling (DNB) is vital to estimation of the course of a hypothetical loss-of-coolant accident for a PWR. DNB is calculated to occur within about a tenth of a second after a postulated instantaneous double-ended break of a large pipe, or a large split. The heat transfer after this time would primarily determine the temperature history of the clad during blowdown and the possibility that clad damage would occur during this phase. It would also determine the effectiveness of removal of heat from the oxide fuel itself and thus the stored energy in the fuel at the time refill of the plenum by ECCS fluid starts (60, p. 1117) (emphasis added).

For example, if the DNB transition can be avoided, the high heat transfer rates associated with nucleate boiling will cause a reduction in the stored energy of the fuel (and hence the potential for heating the cladding) corresponding to an average temperature decline of about 150°F/sec. Each additional second of nucleate boiling during depressurization can permit a delay of about 10 sec in core coolant (ECC) injection (34).

Since the DNB/CHF transition from nucleate boiling to stable film boiling is so important to heat transfer during blowdown, reactor manufacturers have attempted to develop transition models which numerically describe the physical processes taking place. These models have

been the origin of most of the controversy associated with heat transfer estimates during the blowdown period. The models have generally tried to make the transition process resemble nucleate boiling as long as possible, for obvious reasons. Such models have also attempted to incorporate hysteresis-like effects which permitted rewetting with a return to nucleate boiling conditions if appropriate CHF values were reobtained for relatively short periods (on the order of milliseconds). The general effect of most of the vendor models proposed to date would be to retain essentially full nucleate boiling heat transfer during most of the blowdown phase of the LOCA (60), p. 1118).

After reviewing the basis for the models, the Commission chose, perhaps as a result of apparent inconsistencies in the arguments of both the vendors and the regulatory staff, to preclude rewetting following DNB during blowdown, and to require the use of stable film boiling models during the post-CHF period. They stated:

We note the inconsistency of vendor positions that would rely on hysteresis-like effects as the basis for switching criteria such as those above, as compared to other positions calling for instantaneous rewet. We note also the inconsistency of Staff positions to the contrary in both cases. The point remains that there is not adequate understanding of either rewet after CHF or of hysteresis-like effects during flow reversal in a fast transient. The Staff's position has been to approve use of only stable film boiling once DNB has been calculated to occur, even when fluid and clad temperature conditions appropriate to rewet exist. This course is conservative. No less conservative position is justified by the record of the hearing. We concur with the Staff's proposal that the nucleate boiling term of the Westinghouse correlations not be used after DNB is calculated to occur, and that other models incorporate the equivalent assumption of stable film boiling throughout the period after DNB (60, p. 1118) (emphasis added).

The uncertainty associated with CHF and post-CHF heat transfer results from basic inadequacies in the transient and steady-state CHF experimental data (as noted above). No data exists, either steady-state

or transient, for large arrays of 7 x 7 or greater and full length rod bundles with PWR rod diameters. In addition, a principal problem with the available data seems to be related to the lack of adequate correlation between theoretical and numerical analyses, and the experimental programs which have been conducted. Weaknesses of this type have left the scalability of test results an open question and, consequently, the conservatism of correlation models uncertain. More test programs with solid analytical bases, such as the program outlined in reference 34, are needed before the adequacy of AEC or vendor models can apparently be assured in the analysis of this important aspect of the LOCA. However, it should be reemphasized that in the absence of such programs, the AEC appears to have made adequately conservative assumptions in the AC. Data obtained from the recommended test programs would be expected to lead to requirements for heat transfer coefficients which are generally less conservative in the important post-CHF period.

3.2.6 Reflooding rates and the treatment of loop resistance

Reflooding rates are a very critical element in the effectiveness of the LWR-ECCS. As discussed in greater detail in appendices 8 and 9, and the following section of this chapter, the reflood rate is a dominating factor in post-blowdown heat transfer. For the PWR it is the sole method of emergency cooling. In the case of the BWR, reflooding provides the dominant means of ultimately achieving temperature turnaround, and resolution of the LOCA thermal excursion. Thus, it is vitally important that the reflood rate be maintained at as high a value as possible to assure ECCS adequacy.

The reflooding rate is critically affected by the resistance of the primary loop of the reactor through the mechanism of steam binding. As stated in the Commission's Opinion to the AC,

The reflooding rate for pressurized water reactors would be controlled to a large extent by steam binding,

the phenomenon by which the resistance to flow through the reactor system (steam generators, pumps, etc.) of the effluent from the reactor core limits the rate of reflood and, indirectly, the rate of heat removal from the fuel rods. The pumps in their locked rotor condition would typically provide more than half of this resistance to flow so that the stipulation of their being locked is a serious limitation. If the pump rotors were not locked, their resistance to flow would be reduced by 60% (Exhibit 1113, p. 14-10). In their Concluding Statement, Combustion Engineering states that if the pumps were free running during reflood the calculated maximum temperature of the zircaloy cladding would be reduced by 75°F (CE Concluding Statement, p. 3-61) (60, p. 1122).

The factors affecting steam binding and reflooding rates were analyzed as one part of a fault tree investigation of ECCS performance by Brockett, et al. of ANC (10). With respect to reflooding rates they stated:

In calculations of the core reflooding rate, the pressure drop from the core inlet to the core outlet plus the pressure drop from the core outlet back to the downcomer is equated to the head of water in the downcomer. This near balance of water head in the downcomer with backpressure from steam escaping the system has been referred to as the steam binding problem. A 17% error in reflooding rate calculations would be necessary to cause a 10% error in heat transfer coefficients. The three diamonds on the right relate to the supply of water to the downcomer which is a key factor in the reflooding rate calculations. In order for a fault to occur, the predicted downcomer water height would have to be about 40% higher than the actual height (10, p. 319).

Brockett, et al. summarized the three aspects of ECCS performance which could cause the predicted pressure drop to be lower than the actual values (and consequently lead to overestimated, or unconservatively generous, predictions of reflooding rates). They considered the following faults to dominate:

- (1) Analysis Underpredicts Effect of Plugging by ECC Water in Pipes. For plant design in which ECC is injected into the piping, the actual pressure drops will be complex functions of momentum and energy exchanges between the ECC and the steam flowing in the lines. Two-phase pressure drops of real significance in terms of core reflooding rates are most likely to occur during accumulator injection when ECC flow rates are high. The author's

opinion is that if the angle of the ECC injection line is such that the ECC momentum is directed down the pipe the plugging problem would be less than for an ECC line with a 90 degree angle to the inlet piping. Although perhaps not significant, the assumption that one of the two LPIS systems fails to operate, which is customarily assumed in safety analysis, would not be conservative in calculating core reflooding rates provided the downcomer is filled by the accumulator; that is, the additional LPIS flow with both systems operating would not increase the core flooding rate, which is controlled by steam binding, and would contribute to increasing the system pressure drop to the break by additional water plugging.

- (2) Analysis Underpredicts Energy or Mass Transfer from Steam Generator Secondary System to Primary Loop. During core reflooding the fluid from the secondary side of the steam generator has the potential to transfer energy, and in the event of a tube leakage also mass, to the primary system. Transfer of either mass or energy can add significantly to the pressure drop from the upper plenum to the downcomer annulus. Unless the secondary side has depressurized before reflooding is initiated, significant errors in reflooding rates would occur if energy transfer processes are not properly taken into account. Current predictions, which are based on the secondary side of the steam generator being at normal operating temperatures at reflooding initiation, are considered to account for all energy transfer processes. If only a few tubes leak, mass transfer processes can also significantly add to the error in the calculated reflooding rates. To provide a 17% reduction in reflooding rates (at 1.5 in/sec), a tube break area of about 0.003 ft² would be required. Present predictions are based on the assumption that none of the tubes fail.
- (3) Analysis Underpredicts Pump Resistance. During the reflooding phase of the accident the pump is subjected to a superheated steam flow at a rate which considerably exceeds the pump capacity. In this situation the pump is simply a resistance. It is, however, the component with the highest resistance in the operating loop and would cause unacceptable reflooding rates if actual values of pump resistance exceed predicted values by about 50%. Potential sources of errors in evaluating pump resistance include:
 - (1) Error in calculating pump speed
 - (2) Application of an inappropriate pump characteristic curve
 - (3) Compressibility effect not taken into account.

Current licensing criteria require the assumption of a rotor resistance for the pump when ECC performance is being evaluated. This assumption provides a resistance about twice as high as would be obtained on the basis of the predicted pump speed (10, pp. 319-320) (emphasis added).

The revised evaluation modeling criteria of the AC require that plugging of the unbroken reactor coolant pipes be considered complete (i.e., no steam flow is permitted) during the time the accumulators are discharging water into the pipes. The pump condition to be modeled must result in the greatest cladding temperature, considering both cases of either a locked impeller or free running rotor. Coolant core exit flow for PWRs is to be determined on the basis of PWR-FLECHT data relating the fractional flow (carryover fraction) at the core exit plane to the total liquid flow at the core inlet plane. No specification is given for analyzing the very important contribution of steam generator tube leakage to ECCS steam binding, because this subject was ruled to be outside of the scope of the ECCS Hearings (60, pp. 1121-1123).

The CNI have expressed their concern over low reflooding rates, as follows:

The extremely degraded cooling effectiveness expected in modern PWRs is one of the most crucial flaws in the assurances of PWR safety. CNI does not believe the safety problems thus posed can be resolved with the present body of experimental information and with the analytical tools now available for PWRs operating at their design power rating (7, p. 5.23).

In the opinion of CNI:

It is now established that core flooding rates [once] considered as extremely degraded are now very close to the expected conditions for a double-ended PWR inlet line break. There is a widespread feeling in the community of reactor safety engineers that there is presently a relatively small and likely non-existent margin between cooling and non-cooling (7, p. 5.20) (emphasis added).

The AEC denies this allegation, stating:

...the evidence shows that because of the conservatisms listed above (maximized stored fuel energy at the beginning of

reflood and treatment of "accumulator bypass" to minimize water remaining in the vessel at the end of blowdown) and because the reactor reflooding rates predicted by current reflood codes are for average, not oscillatory, system thermal-hydraulic response, the calculated reflooding rates are lower than would be expected to occur in reality (6, p. 204).

It is significant to note, however, that a substantial decrease has taken place in AEC estimates of flooding rates from their "intended" rates of 6 to 11 inches per second (60, p. 1092), even in the brief period since the IAC was published. The AEC Supplemental Testimony (4) compares calculations of nominal flooding rates for typical PWR designs from each vendor. These current estimates range (for average flood rates) from less than 1 in/sec for a Westinghouse 4-loop system with an ice condenser type containment (4, p. 14-7) to values as high as 2 in/sec for B & W vent-valve plants (4, p. 14-9). "Nominal" estimates, for a Westinghouse 4-loop system with a dry containment, predict average flood rates of about 1.3 in/sec.

The current flooding rate estimates, ranging from slightly less than 1 in/sec to a high of 2 in/sec, should be compared with earlier statements presented in the AEC initial Direct Testimony (8). At that time, vent valve reactors were predicted to have "high" flooding rates of "about six inches/second" while flooding rates of "about 1 inch/second" were considered "low" (8, p. 2-21). Thus, it appears that even in the short period (approximately 10 months) between publications of the AEC initial Direct Testimony and their Supplemental Testimony there was a marked narrowing of the range of "realistic" estimates of reflood rates towards the "low" estimate of about 1 in/sec.

As observed in section A9.4, flooding rates of 1 in/sec are critically low. The PWR-FLECHT test results showed that it was difficult to control fuel rod thermal excursions at 1 in/sec. At 0.6 in/sec, even when the temperature at the end of blowdown was relatively low (i.e., 1600°F), temperature turnaround could not be achieved for rods with an initial linear

power density of 1.24 Kw/ft (comparable to steady-state operational values of 18 Kw/ft). Thus at estimated values of flooding rates of 1 in/sec, there is an uncomfortably small margin of coolability remaining.

The margin is made even more uncomfortable when it is recognized that there is essentially no valid experimental data against which to compare the calculational results obtained for system flooding rates. However, there are test programs underway to examine specific limited elements of the loop resistance/reflood rate prediction problem. Tests currently being conducted at ANC, Combustion Engineering and Westinghouse, according to an informed AEC Regulatory Staff member*, are investigating the counter-current flow of water-steam mixtures. It has been reported* that the tests will show the excessive conservatism of the assumption of no steam flow (plugging) of the broken cold leg during ECC injection and the requirement for total "accumulator bypass" (i.e., loss of all ECC fluid injected during blowdown). If these assumptions can be shown to be excessively conservative, the magnitude of the reflood rate minimum in its time history (i.e., the 1/2 in/sec portion of the "nominal" reflood rate predictions) and the duration of the dryout period (the period between the calculated time of exhaustion of the core coolant and the time at which the bottom of the core recovers) would be less critical. Though these changes would certainly improve post-blowdown heat transfer prior to reflood, the tests are not relevant to resolving the dominant questions associated with the magnitude of flooding rates during the reflood period, which are now uncomfortably close to values bordering on the limits of thermal excursion controllability.

C. George Lawson, a heat transfer expert from ORNL and the author of the 1968 ORNL critical review of the ECCS (27), was reported as having stated his concern over the reflood rate problem in the following manner:

As an experimentalist, a clear demonstration of coolability by wide margins would be necessary to satisfy his uncertainties

* Mattson, Roger J., personal communication, 8 August 1973.

regarding ECCS capability. In other words, cooling by narrow margins would have to be regarded by Lawson as an essentially uncoolable situation (29, p. 19).

The Commission, in its Opinion to the AC, supported Lawson's conclusions. They presented their opinion in their description of the LOCA physical processes, as follows:

The temperature excursion would eventually be terminated as the ECCS begins to reflood the core. Both PWR's and BWR's have ECC systems in which water would reflood the reactor. In BWR's the reflood would be provided by accumulation of water from the low pressure injection system and the core spray system. Direct core spray is discussed below. To accomplish reflood in a reasonable time, the rate at which the emergency cooling water would encroach on the core (the reflood rate) must be high enough to provide a heat transfer rate from the core that would be sufficient to counter the heat input rate from decay heat and from zircaloy oxidation. The Commission believes that the calculated reflood rate should have a substantial margin over the rate that is just sufficient to turn the temperature excursion around in a short time.

As the cooling water reaches the hot core much of it would be converted to steam, and it is this steam together with entrained water droplets that would provide the initial cooling of the hotter regions of the core. For the reflood water to continue entering the core it must displace the steam, which would have to escape from the reactor vessel and find its way into the containment atmosphere. In the pressurized water reactors the steam would have to flow through the steam generator and pump to escape through a cold leg break; the reduction of reflood rate by the relatively high resistance to flow of this path is called "steam binding". Steam binding would severely limit the rate of reflooding the core, reducing it from an intended 6 to 11 inches per second to from 1.0 to 2.5 inches per second, depending on the reactor design. The rule we announce considers all the evidence in the record on this important subject of steam binding and provides an acceptable overall assurance of ECCS effectiveness. The inquiry, however, should not end there. Thus the Commission urges the pressurized water reactor manufacturers to seek out design changes that would overcome steam binding. This same point of view is reflected in the September 10, 1973, letter of the Advisory Committee on Reactor Safeguards.

Boiling water reactors would not be subject to steam binding, because their system design provides a more direct path for the steam to escape, but the same requirement for rapid reflood would have to be met if excessive clad damage were to be avoided. Boiling water reactors do have a core spray system that would start about 30 seconds after occurrence of the break, but its cooling effect on the central rods of a fuel bundle might be insufficient in itself to prevent exceeding the temperature limits we have set. The occurrence of reflooding within three minutes after a postulated break of the recirculation line would terminate the excursion (60, p. 1092) (emphasis added).

At flood rates less than 2-4 in/sec, the PWR-FLECHT results indicate a dramatic reduction in fuel rod cooling occurs (see appendix 9, sec. A9.4, for detailed analysis). To allow margins of coolability which would be sufficient to cover reflood heat transfer uncertainties with greater confidence, it would appear that reflood rates of 6 in/sec or more would be necessary. In view of the added uncertainties over the possibility of reflood rate reduction through steam generator tube rupture, an unevaluated hazard in the ECCS hearings (reviewed briefly in appendix 9, sec. A9.4), it seems desirable to establish specific criterion related to reflood rates (or perhaps more generally a specific, demonstrable reflood heat transfer coefficients criterion), which is probably the most critical element of PWR coolability in the event of a LOCA. Thus, the author would support the Commission's recommendation urging PWR manufacturers to "seek out design changes that would overcome steam binding" (60, p. 1092).

3.2.7 Reflood/core spray heat transfer

The ultimate function of the ECCS is post-blowdown heat removal, accomplished through the reflood mechanism for PWRs or the combined core spray reflood mechanisms for BWRs. Thus it is extremely important that the reflood/core spray heat transfer mechanisms be well understood and conservatively modeled to insure that successful ECCS performance will be provided.

To successfully reverse the LOCA thermal transient, emergency cooling water must be supplied to the reactor core at a rate (referred to as the reflood/core spray rate) which is large enough to counter the heat input from decay heat, zircaloy oxidation, and the remaining initial stored energy in the fuel. It is intuitively obvious that increasing the reflooding rate will improve the heat transfer in the reflooding process. Previous sections have dealt with problems associated with mechanisms which tend to reduce or limit reflooding rates. This section will analyze the state-of-knowledge of the reflood/core spray heat transfer mechanisms themselves.

Because the physical processes occurring during ECC are quite complex, the principal means of developing modeling tools for their evaluation has been through empirical methods. Attempts have been made to isolate and examine the elements of the phenomena of reflooding and core spray through a series of experiments, the Full Length Emergency Cooling Heat Transfer (FLECHT) test programs. The FLECHT programs have formed the basis for the post-blowdown heat transfer models prescribed in all of the AEC criteria to date. Though there have been some modifications in interpretation of the program results over the course of the hearings, the basic evaluation model methodology is still fundamentally dependent upon the validity and adequacy of the FLECHT data.

Two separate test programs were conducted, one for boiling water reactors (BWR-FLECHT) and another for pressurized water reactors (PWR-FLECHT). The two test programs, conducted by GE and Westinghouse respectively under subcontract to the Idaho Nuclear Corporation, though different in procedural detail were similar in many general ways. In the tests electrically heated, full length fuel rods were tested in bundle configurations simulating reactor fuel rod bundles for BWRs and PWRs which were contemporary to the test period. The fuel rod cladding material was fabricated of either stainless steel or zircaloy. The rods were heated by electrical resistance heaters designed to mock up operational rod axial

power distribution (a chopped cosine power distribution along the axial length of the rod).

The time history for the electrical power supplied to the rod bundles was programmed to simulate bundle decay heat in an operating reactor with the reactor operating at full power at the time of the LOCA shutdown.

The tests were conducted in a parametric fashion. Analyses of the calculated values of coolant application rates, initial temperatures, peak operating power, and time sequences of coolant application were the basis for the determination of the ranges for each of these parameters. The test series for both BWRs and PWRs included several hundred tests over the full range of parameters, various rod cladding materials, and several heater designs.

As discussed in detail in appendix 8, both the BWR and PWR-FLECHT programs had many problems and weaknesses. Critical tests in the programs were too often poorly designed, marred by malfunctioning test equipment or poorly analyzed. Consequently, critical data were frequently indeterminate in form and suffered from poor evaluation. As a result, the painful conclusion must be drawn that some of the critical FLECHT results upon which post-blowdown heat transfer models depend heavily are a source of unending controversy.

On the positive side, the FLECHT tests did demonstrate that simulated LOCA thermal transients could be terminated under very severe conditions. For the BWRs, temperature turnaround was demonstrated for tests with rod powers in excess of limiting IAC conditions (probably induced by heater failures) (Test Zr2K). For the PWRs, limiting values of reflood rates of the order of 1 in/sec were demonstrated, below which the ECCS may not function successfully. For BWR core spray tests, and for PWR reflood rates above limiting values, however, no readily identifiable evidence of "run-away" metal-water energy release was observed, even though peak temperatures exceeded the IAC limit of 2300°F.

Though it would appear that conservative core spray and reflood heat transfer analysis methods may be derived from FLECHT test results, the evidence is weak that current models will give completely conservative results. In fact, the Commission's AC opinion states that:

The accuracy of the FLECHT-determined heat transfer coefficients has been examined several times. (Cf. the review in the Babcock and Wilcox Concluding Statement, pp. 202-204.) Westinghouse estimated a possible uncertainty of 12% in the coefficients (Trans. page 6878). The Aerojet Nuclear Company concluded "that the FLECHT data currently represent a best estimate of the heat transfer that will occur in a large undistorted core." They also concluded that an allowance of up to 20% may be needed "to bound the data due to experimental and inferential errors." (Exhibit 1113, p. 17-14) The Commission approves of the use of the FLECHT data for calculating PWR reflood heat transfer, but notes that these will be more nearly "best estimate" calculations than bounding calculations (60, p. 1124) (emphasis added).

Thus questionable conservatism associated with calculated reflood heat transfer based upon PWR-FLECHT results is acknowledged. Similar difficulties are acknowledged with BWR-FLECHT heat transfer coefficients. The Commission's AC opinion states:

The BWR-FLECHT convective heat transfer coefficients were determined from the residue of a thermal balance after all of the known inputs and outputs were calculated. The factors considered were the electrical heat input, the rate of change of the heat content of the rods as calculated from their temperature history, and the calculated radiation from the rods to each other and to the channel walls. The residue from these inputs and outputs was ascribed to convective heat transfer. The convective heat transfer coefficients so determined could not be very accurate because their calculation involved taking the difference between two large numbers. The coefficients so obtained are small and are about what one would expect from the mechanisms of natural convection and radiation to steam (Exhibit 1113, p. 16-14).

There has been a great deal of criticism of the BWR-FLECHT tests, particularly by the Consolidated National Intervenors (Exhibit 1041, Chapter 5), and both General Electric and Regulatory Staff have defended them (Closing Statements). However, for

the purpose of calculating the maximum cladding temperature, only the derived heat transfer coefficients are of any great importance. The values obtained have always been known to have a high statistical error; furthermore the values are low and reasonable, and there seems little to be gained by renewing the controversy over the manner of conducting and interpreting all features of the tests.

The high but inevitable statistical error of the coefficients for the inner rods (1.5 ± 1.0 BTU/hr-ft²-°F) is bothersome and leads to an estimated error band of as much as $\pm 200^\circ\text{F}$ in the calculated peak temperature in some circumstances (Exhibit 1113, p. 16-36) (60, pp. 1125, 1126) (emphasis added).

A large degree of uncertainty is therefore acknowledged to be associated with the use of FLECHT derived heat transfer parameters in estimating LOCA temperature histories. Some additional work on the analysis of the FLECHT data with respect to the application of the data to the evaluation models would appear to be appropriate in order to achieve greater conservatism. Additionally, FLECHT tests to date have been conducted at power levels lower than AC specification. Some additional testing of current bundle designs for design power level temperature excursions would be desirable as well as more tests utilizing zircaloy rods (see appendix 8, section A8.1) and improved blockage simulation. With these additional tests, it should be possible to demonstrate the conservatism of the core spray/reflood heat transfer evaluation models sufficiently to satisfy the requirements of "reasonable men."

However, with the currently acknowledged uncertainties in the FLECHT data, the desirability of a conservatively high reflooding rate is clearly apparent. As discussed in the previous section, reflooding rates of current PWR reactor designs average from less than 1 in/sec up to approximately 2 in/sec. These flooding rates are critically low. As discussed in greater detail in appendix 9, section A9.4, at flooding rates less than 2 to 4 in/sec, the PWR-FLECHT results indicate a dramatic reduction in fuel rod cooling capacity occurs. Current design flooding rates are uncomfortably lower than these transition rates. To be conservative, higher flooding rates, about 6 in/sec, would be desirable.

BWR flooding rates are relatively high, typically about 4 in/sec. Moreover, the heat transfer coefficients (HTCs) accredited to these reflood rates (25 B/hr-ft²-°F) appear to be conservatively specified. Nevertheless, reflooding rates on the order of 6 in/sec would be desirable even for the BWRs, as well as PWRs. Again, we note our support for the Commission's position of urging manufacturers to "seek out design changes" which would result in higher reflooding rates (60, p. 1092).

3.2.8 Analytical models and numerical methods

The Acceptance Criteria prescribe only the conditions associated with the calculated reactor system response to a LOCA. Consequently, the major part of the AC specifications are directed at prescription of "Required and Acceptable Features of Evaluation Models" for evaluation of the ECCS performance. We have dealt with the conservatism of individual elements of the more significant parts of the AC specifications for evaluation models previously in this section. In this portion we will consider the integration of the individual parts into numerical codes for overall system evaluation.

The scope of this study did not permit an extensive investigation of the specific details of the various codes available for calculation of ECCS performance. It is sufficient to observe that the codes themselves are very elaborate, lengthy, complex and long running. In this regard, the observation of Alvin Weinberg when he was Director of ORNL are well taken:

With respect to the criteria themselves, I have only one point to make. As an old-timer who grew up in this business before the computing machine dominated it so completely, I have a basic distrust of very elaborate calculations of complex situations, especially where the calculations have not been checked by full-scale experiments. As you know, much of our trust in the ECCS depends on the reliability of complex codes. It seems to me--when the consequences of failure are serious--then the ability of the codes to arrive at a conservative prediction must be verified in experiments of complexity and scale approaching those of the

system being calculated. I therefore believe that serious consideration should be given first to cross-checking different codes and then to verifying ECCS computations by experiments on a large scale and, if necessary, on full scale. This is expensive, but there is precedent for such experimentation--for example in the full scale tests on COMET and nuclear weapons (29). (Discussed more fully in section A9.1 of appendix 9.)

The author's personal experience with large complex codes of this type leads him to agree with Weinberg that there is a critical need for large scale testing against which to validate and mature the ECCS performance codes. The test program which currently comes closest to satisfying the need for large scale ECCS testing is the long delayed LOFT experiment, a test of ECCS operation for a 55 MW_t multi-loop PWR nuclear steam supply.* (System testing of LOFT with nuclear fuel is not expected to begin until about 1976.) LOFT will be the first significant simulation of a PWR ECCS to be conducted with a reasonable test configuration and scale. Though at 55 MW_t the reactor is only 1/60 of the scale of a current 1000 MW_e nuclear power plant (3300 MW_t), LOFT is still a vitally important test. Whether 1/60 scaling is wholly adequate for full scale ECCS simulation is uncertain. The AEC has stated:

For lack of full-scale LOCA experience -- a fortunate deficiency -- and because of the impracticability and hazard of full-scale experiments, scaled experiments will have to be used. This means that the scaling laws have to be investigated (8, p. 1-27).

From this and other related AEC statements it appears that no significant study of scale effects for ECC system tests has been made (or at least none has been published). Consequently, the required scaling for adequate system simulation cannot be specified with confidence. But it is a truism that for a system of the complexity of a nuclear reactor undergoing a LOCA, the closer the test is to full scale the more believable the results will be.

* A listing of ECCS related experimental programs on a world-wide basis is given in appendix 4.

Though large or full scale testing is expensive, there is ample precedent for it. In fact, nearly all recent large complex systems where survivability has been important, from ballistic missiles to jet airliners, have been tested extensively in full scale (and frequently to destruction) to demonstrate their overall design conservatism. On the basis of such precedents, large scale tests, though admittedly expensive, would certainly appear to be cost effective for the nuclear power industry. It has been estimated that in the next 30 years more than 500 nuclear power plants, of approximately 1000 MW_t capacity each, will be in operation. To argue over the practicability of ECCS testing for a large scale nuclear system on the basis of expense under these circumstances seems absurd.

If, on the other hand, the principal drawback to large or full scale testing is associated with the hazards of the experiment, then the argument becomes unsettling. It seems unlikely that an adequate location cannot be found within existing AEC test sites in the U.S. for which the hazards to the public of large scale ECCS testing could not be reduced to acceptable levels. If this cannot be done, then it appears that a double standard is being applied with respect to public exposure to risks from operating reactors: existing reactors represent potential sources of LOCA experience at sites far less remote than test sites where the AEC is reluctant to conduct full scale tests.

Therefore, it seems that the constraints of the practicability and hazards of testing, on a sufficiently large scale to overcome major scaling uncertainties, must be overcome. Testing at a larger scale than LOFT seems practical, desirable, and urgently needed. Consequently, a larger scale test program should be planned and conducted by the AEC as expeditiously as possible.

The AC specifies the requirement for documentation of the complete evaluation models for the ECCS for all vendors. This is a very commendable and important requirement. The CNI made a very strong criticism of the BWR LOCA model on the basis that there were no independent models of

similar scope, available to the AEC, against which to check the General Electric codes. As Weinberg implies, this is a very legitimate criticism. It casts no aspersions on GE to suggest that cross-checking of the BWR model should be performed by comparison with another independent code of equal stature. Anyone who has had experience with large complicated numerical models of systems must be aware that it is essentially impossible to construct one without programming errors. No doubt a great many cross-checks of individual code sub-routines have been made by GE, where such checks are possible. Nevertheless subsystem verification, though important, is no substitute for full scale exercising of the overall model. It can be stated unequivocally that large codes the size of the BWR-LOCA model need multiple cross-checking.

The above comments should not be taken as being applicable exclusively to GE. Thorough cross-checking of all of the PWR codes is also extremely important. Aerojet Nuclear Corporation (ANC) is currently conducting and coordinating a program of comparative calculations of several "standard" problems in which each of the PWR vendors is participating. This program has the potential of providing a very significant service -- if adequate analysis of the results is made. Consideration should be given to providing supplemental funding for vendor calculations to assure that adequate cross-checking is permitted and supported.

In the author's opinion, one more important observation needs to be made with respect to ECCS evaluation model documentation. The AC requirement for documentation is unassailable in its desirability and value. However, when the vendor documentation is furnished to the AEC, the use of proprietary documentation for ECCS models by any vendor should not be allowed. For system elements as critical to public safety as the ECCS, all supporting documentation should be in the public domain. Public evaluation of the conservatism of ECCS designs is essential to the continued growth of nuclear power in the U.S. The public's evaluation must remain incomplete if proprietary documentation of the model is

accepted by the AEC. Proprietary documentation of ECCS models is counter-productive to all concerned with the growth of nuclear power and should be considered unacceptable as evidence of model adequacy.

3.3 Relative Importance of Parameters Affecting Thermal Response

In the preceding discussion of the relative conservatism of the PR specification of LOCA parameters, no attempt was made to rank the parameters in terms of their relative importance to system thermal response. Estimates of parameter ranking have been made on the basis of parametric analyses which have been conducted by the vendors and the AEC. As a result of their convictions that the IAC was "excessively" conservative, the vendors have conducted "best estimate" analyses in which LOCA thermal response was evaluated using values of critical parameters which were less pessimistic than those required by the IAC. The results achieved in the vendor studies may not be considered truly "best estimate" in a statistical sense, since, as observed by the AEC, they do not account for the entire range of uncertainties in input parameters, nor do they "account for propagation of uncertainties" or "provide confidence bands for the final calculated results" (4, p. 2.1). However, the results give at least qualitative indications of the relative importance of the parameters investigated and have been extrapolated, for this report, to provide quantitative estimates of thermal response for more conservative values of the parameters.

3.3.1 Results of parametric analyses

Results of several parametric analyses have been reviewed (e.g., Westinghouse, 31; GE, 32 and 27 [Sec. G]; ANC, 10; AEC, 4, Chap. 10) and some of the more significant results are reproduced and discussed below.* Table 3.1 lists the results of a GE study. An analysis of 12 parameters showed that the four listed (duration of nucleate boiling, heat transfer during lower plenum flashing, critical flow rate for liquid

* A detailed analysis of the results is presented in appendix 10.

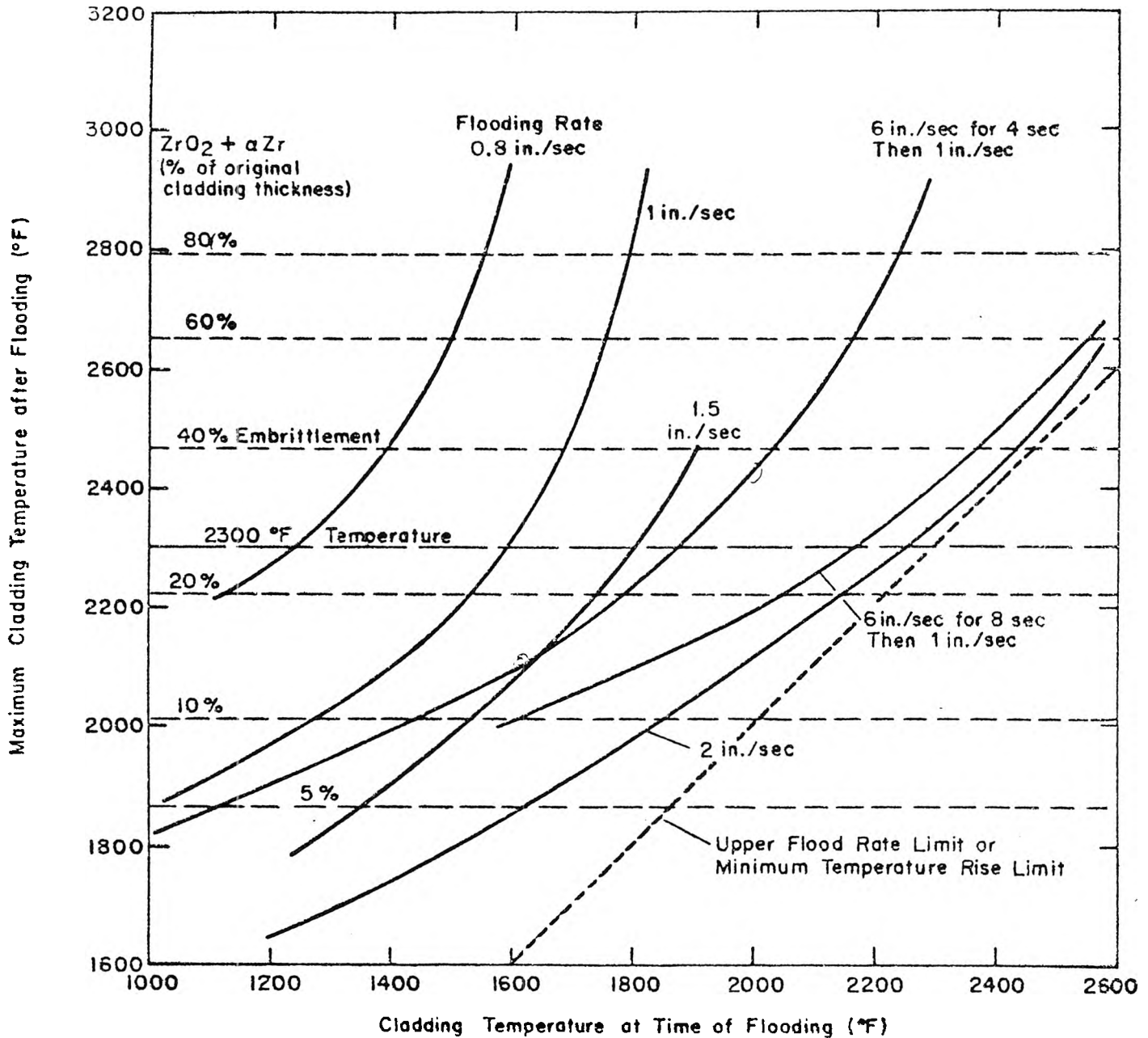
Table 3.1

Sensitivity of Peak Clad Temperature During Core Spray Operation to Specified Variables		
Item from Appendix III of Exhibit 1069	Decrease in Peak Clad Temperature ($^{\circ}$ F) From Base Case	Discussion
(3) Duration of Nucleate Boiling	Approx. 120	A previous sensitivity study on this item showed that the peak clad temperature decreased 40° F for each second of delay of CHF. It is more important to note that for the expected duration of nucleate boiling the rods will rewet.
(3) Heat Transfer During Lower Plenum Flashing	Approx. 800	Rewetting of the rods would decrease the cladding temperature to nearly saturation temperature by the time the fuel uncovers. The peak cladding temperature during core spray operation in this case is merely the heatup during the time of fuel uncover.* This item renders insignificant all items that pertain to the portion of the accident prior to uncover.
(5) Critical Flow Rate for Liquid	Approx. 300	The sensitivity of this item has been investigated using the models of NEDO-10329. Changing the critical flow rate essentially changes the time-scale of blowdown without affecting the decay power significantly. Essentially the same rate of energy removal results with a decreased lower plenum flashing inventory loss.
(6) Cooling Systems Operable	Approx. 150	Analyses have been made using the models of NEDO-10329 for the case in which all cooling systems are operable, as well as for cases with single failure of an active component.

* The peak clad temperature over the entire transient for this case is about 1300° F, and occurs at the start of lower plenum flashing. Since core heatup after lower plenum flashing begins at only about 500° F, the clad temperature at core reflood will be only about 1100° F. It is this latter figure--the peak clad temperature during core spray operation--that is compared with the base case in the above table.

Figure 3.1

PWR-ECC Performance Map



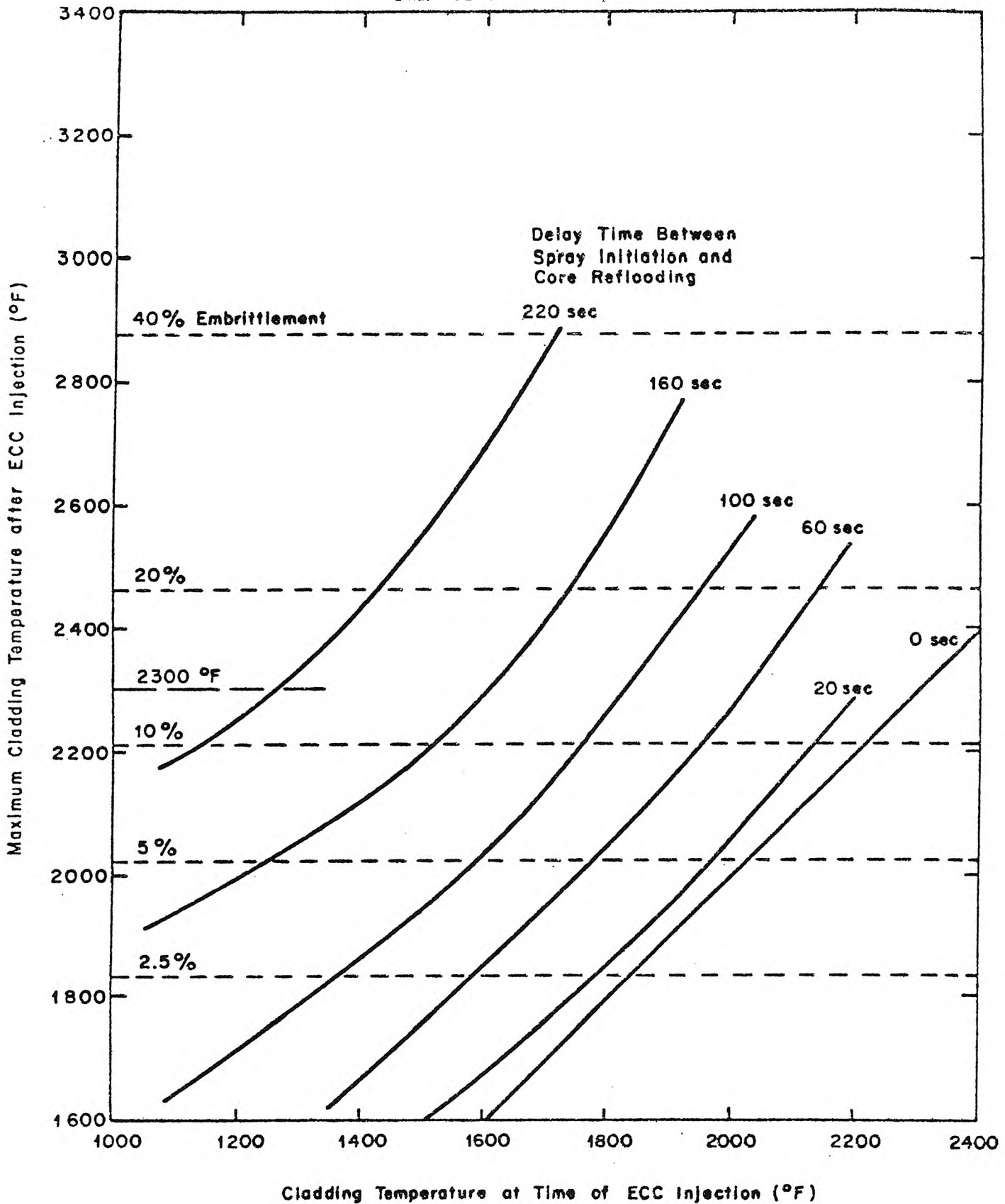
ANC-A-861-H

Fig. 12 PWR-ECC performance map obtained through use of PWR-FLECHT data.

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(10 and the American Nuclear Society)

Figure 3.2
BWR Performance Map



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(break flow), and maintaining cooling system operatibility without the time lag required by the AEC "worst single failure" criterion) were the most significant for the LOCA thermal response. In addition to the listed variables the analysis considered parameters such as: axial and radial power factors, local power distribution, and decay heat generation rate.

Analysis of the results indicates that the majority of the referenced parameters of table 3.1 are functionally related to the heat transfer mechanism of nucleate boiling for their effects. On the basis of extrapolation of the results to more conservative values of the parameters investigated, an increase in peak temperatures on the order of 100°F might be expected from application of the more pessimistic parameters discussed previously in this chapter. That is, if the time to departure from nucleate boiling (DNB) was decreased by several seconds, as a more conservative analysis with AC prescribed parameters would indicate might be possible, an increase in temperature above IAC predictions on the order of 100°F might be expected, based upon extrapolation of the results of table 3.1.

Results of an analysis conducted independently by the Aerojet Nuclear Corporation (ANC) (10) are shown in figures 3.1 and 3.2 for PWRs and BWRs respectively. Results are given in terms of calculated maximum temperatures as a function of the cladding temperature at the time ECC application is initiated (i.e., at the time of reflooding for PWRs or core spray initiation for BWRs) for various values of flooding rate for PWRs or delay between spray initiation and time to core reflooding for BWRs.

The results for PWRs imply that as long as temperatures at the end of blowdown are less than the 2200°F criteria limits, and flooding rates are in excess of 2 in/sec, maintaining peak temperatures below criteria limits should not be a serious problem. However, for flooding rates less than 1 in/sec and temperatures in excess of 1600°F at the end of blowdown,

peak temperatures will exceed criteria limits. Moreover, at these flooding rates, if temperatures exceed 1800°F at flooding initiation, the results may not be controllable. Thus, the importance and inter-relationship of flooding rate and blowdown parameters are shown.

Similar results are shown for BWRs in figure 3.2. For a reasonable range of temperatures at the time of initiation of core spray (clad temperatures less than 1800°F), delay times before core reflooding on the order of 2 minutes are possible before temperatures in excess of criteria standards are developed. The importance of minimizing the time between core spray initiation and reflooding is shown, for a given initial clad temperature, in the nonlinear increase in peak temperatures with increasing delay time between the two ECC operations.

The influence of other parameters investigated in the ANC study is shown, for PWRs, in the results of table 3.2. In the words of the ANC authors, the implications of the table are described as:

As an aid in estimating the effect of several other parameters, results from other sensitivity studies are presented in Table 1. [Table 3.2] This table shows the percent change in the cladding temperature rise and embrittlement for two points on Figure 12 [Figure 3.1]. These points are for 2000 and 1600°F initial temperatures for a flooding rate of 6 in/sec for 4 seconds followed by a flooding rate of 1 in/sec. By utilizing the information in Table I [Table 3.2] and the curves of Figure 12, new performance maps could be constructed.

The largest changes occur in the 2000°F column because the metal-water reaction energy is more significant at this temperature than for the 1600°F temperature at reflood initiation. The parameter which caused the greatest effect was the initial power. Next, in the order of importance, are: (a) metal-water reaction energy multiplying factor; (b) the time at which reflooding begins; and (c) ZrO₂ thickness.

Another important parameter affecting the relationship between the temperature at the time of reflooding initiation and the maximum temperature for a given reflooding rate is containment pressure. The containment pressure affects the flooding rate as well as the heat transfer for a specific flooding rate.

Table 3.2 ANS Sensitivity Study

TABLE I
SENSITIVITY STUDY SHOWING EFFECT OF PARAMETER CHANGES ON RESULTS IN FIGURE 12

Parameter Value	Temperature of 2000°F at time of reflooding		Temperature of 1600°F at time of reflooding	
	Temperature Rise (% Change)	Cladding Embrittlement (% Change)	Temperature Rise (% Change)	Cladding Embrittlement (% Change)
Baker-Just multiplication factor				
= 0.5	-21	-21	- 6	- 9
= 1.0	55	75	9	12
Initial power (kw/ft) = 1.0	-56	-50	-45	-61
= 1.4	106	170	53	101
ZrO ₂ thickness (in.) = 0.001	-13	- 9	- 9	- 8
= 0.00001	1	2	2	2
7-sec delay in time to initiate reflooding{a}	-16	-16	-10	-15

{a} Delays in flooding initiation result in a reduced temperature rise at any given temperature at reflooding initiation because the decay power is decreased.

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Table 3.3 AEC Sensitivity Study

Table 10.6

Study No. 2

Peak Cladding Temperature

Linear Power Density (kw/ft)			11.1	11.1	11.1	14.1	14.1	14.1	17.1	17.1	17.1
Rupture Time Seconds			22.5	7.0	3.0	22.5	7.0	3.0	22.5	7.0	3.0
fhr*	fhb**	fkf***									
.6	.5	.125	1577.1	1925.7	2012.0	****	-	-	-	-	-
.6	.5	.25	1587.8	1913.4	1975.7	-	-	-	-	-	-
.6	.5	.5	1605.0	1894.6	1931.9	-	-	-	-	-	-
.6	.5	1.0	1628.5	1869.7	1886.1	-	-	-	-	-	-
.6	1.0	.125	1577.1	1857.7	1912.5	-	-	-	-	-	-
.6	1.0	.25	1587.8	1838.1	1878.2	-	-	-	-	-	-
.6	1.0	.5	1605.0	1807.8	1831.8	-	-	-	-	-	-
.6	1.0	1.0	1628.5	1766.0	1775.8	-	-	-	-	-	-
.8	.5	.125	1362.2	1664.3	1699.2	1691.1	-	-	-	-	-
.8	.5	.25	1381.1	1664.2	1693.3	1698.1	-	-	-	-	-
.8	.5	.5	1409.2	1662.2	1682.8	1725.1	-	-	-	-	-
.8	.5	1.0	1443.8	1657.1	1668.4	1765.2	-	-	-	-	-
.8	1.0	.125	1362.2	1625.6	1657.4	1681.1	-	-	-	-	-
.8	1.0	.25	1381.1	1617.7	1664.0	1698.1	-	-	-	-	-
.8	1.0	.5	1409.2	1602.4	1620.5	1725.1	-	-	-	-	-
.8	1.0	1.0	1443.8	1576.2	1584.3	1762.0	2039.1	2074.8	-	-	-
1.0	.5	.125	1262.9	1574.6	1593.4	1522.4	2091.4	-	1925.0	-	-
1.0	.5	.25	1283.0	1572.7	1593.5	1541.1	2051.5	2355.3	1950.2	-	-
1.0	.5	.5	1316.3	1563.7	1584.3	1570.3	2004.2	2083.2	1993.7	-	-
1.0	.5	1.0	1356.4	1563.8	1556.8	1609.1	1955.9	1986.8	2060.2	-	-
1.0	1.0	.125	1262.9	1531.2	1540.8	1522.4	1919.5	2008.8	1925.0	-	-
1.0	1.0	.25	1282.9	1522.4	1535.1	1541.1	1889.0	1949.6	1950.2	-	-
1.0	1.0	.5	1316.3	1502.4	1514.9	1570.3	1844.8	1879.0	1993.7	-	-
1.0	1.0	1.0	1356.4	1489.4	1466.9	1609.1	1788.2	1801.6	2060.2	-	-
1.2	.5	.125	1262.9	1559.6	1593.4	1430.3	1874.3	1962.6	1722.2	-	-
1.2	.5	.25	1270.6	1565.3	1593.5	1442.7	1858.2	1924.6	1743.6	-	-
1.2	.5	.5	1282.6	1564.0	1584.3	1476.7	1836.3	1879.0	1778.2	-	-
1.2	.5	1.0	1297.7	1546.1	1556.8	1519.8	1815.6	1836.7	1826.4	-	-
1.2	1.0	.125	1262.9	1513.0	1540.8	1430.3	1775.3	1831.9	1722.2	-	-
1.2	1.0	.25	1270.6	1507.9	1535.1	1442.7	1752.9	1796.6	1743.6	-	-
1.2	1.0	.5	1282.6	1495.7	1514.9	1476.7	1721.4	1748.6	1778.2	-	-
1.2	1.0	1.0	1297.7	1457.5	1466.9	1519.8	1681.6	1692.9	1826.4	2256.4	2423.9

fhr* - Reflood Heat Transfer Coefficient Multiplier, applies to calculation only after rupture.

fhb** - Blowdown Heat Transfer Coefficient Multiplier, applies to calculation only after rupture.

fkf*** - Helium Conductivity Multiplier, applies to calculation only after rupture.

**** - Means Clad Temperature reached melting.

As an example of this effect, if the core pressure is 25 psia instead of 60 psia and the reflooding rate is 1 in/sec, a 1500°F temperature at reflood initiation would result in a maximum temperature 500°F above the value obtained from Figure 12 [Figure 3.1] (emphasis added)(10, pp. 325,326).

Results of an AEC study of LOCA parameters for a PWR study (discussed in greater detail in appendix 10) are presented in table 3.3 (reproduced from 4, table 10.6-revised). The AEC study investigated effects of linear rod power density, time of swelling and rupture for the rod, reflood heat transfer coefficients (HTC), blowdown HTC, and gas gap conductivity (through the helium conductivity multiplier for the gas in the fuel-cladding gap). Several significant observations can be made from the results presented.

For example, the AEC parametric study shows that at low power levels (11.1 Kw/ft), relatively minor perturbations in any of the parameters were shown to be tolerable -- producing about 50-100°F changes in the peak temperature. However, large perturbations in parameters, such as major reductions in gap conductance through early rupture time or changes in linear rod power levels, produced important changes in peak temperatures (from 100 to 500°F). Such changes are barely tolerable under the most ideal conditions, and were basically intolerable under essentially all conditions investigated which were off-normal (or were otherwise non-ideal). At rod linear power density levels greater than 11.1 Kw/ft (considerably below current design peak linear rod power densities of 18 - 19 Kw/ft) meltdown occurred at essentially all off-normal operating conditions investigated, shown in table 3.3 by elements marked with a dash. Moreover, the results presented indicate that the thermal response of the rods is a strongly non-linear function of temperature. As peak temperatures approach 2000°F, normally minor perturbations in heat transfer related variables induce temperature excursions which are increasingly difficult to control. This appears to be directly related to energy input to the system from metal-water reactions at about 2000°F and above.

Under such circumstances, the nominal (one inch per second) reflood heat transfer rates are stressed to their limits. In fact, the results indicate that under design basis accident power conditions (18 - 19 Kw/ft), currently anticipated flood rates will be inadequate to assure that melt-down will not occur over a relatively large fraction of the core -- assuming the basic accuracy of the AEC's parametric study.

The results give a strong indication that metal-water reactions can produce serious (and undesirable) synergistic effects on LOCA thermal excursions if rod temperatures exceed about 2000°F, in the absence of higher reflood HTC's. This temperature is below the AC limit. It would appear that the application of the revised criteria for modeling gap conductance and rod swelling and rupture to reactors of current design might show that operational power limits reductions may be required to prevent similar uncontrollable temperature excursions.

Vendors have criticized the results of this AEC parametric analysis, as shown in figure 3.3, as being unrepresentative of overall core response to the LOCA. While it is true that swelling and rupture are localized phenomena on a rod and also that rods with linear power ratings as high as 18 - 19 Kw/ft represent a small fraction of the total rods in the core (average linear power density is about 7 Kw/ft), no statistical model of the distribution of swelling and rupture in the core is currently acceptable to AEC. Consequently, the AEC requires that the effects of the temperature excursion be calculated for this singular (but probably not unique) core element (a swollen, ruptured rod with a high peaking factor) as though it applied for the entire core, in an explicitly conservative fashion.

Since the determination of how extensive melting might be throughout the core is uncertain under these circumstances, the results of table 3.3 are a source of concern. It should be emphasized that in this instance, the concern is centered around two areas; the relatively low rod linear power density at which melting becomes a problem, and the relatively low

temperature (on the order of 2000°F) at which the thermal excursion appears to approach uncontrollability. In addition, the results indicate the relative magnitude of temperature changes resulting from changes in the parameters of gap conductance and blowdown and reflood heat transfer coefficients (within the limits of temperature excursion controllability). The significance of these parameters will be analyzed in more detail subsequently.

3.3.2 Influence of parameter variations on the relative thermal response of the system

As discussed briefly in the previous section, there are some problems in making quantitative extrapolations from the vendor analyses in the direction of more conservative application of elements of ECCS evaluation models. However, on the assumption that such extrapolations can legitimately be made, and with the support of the AEC parametric analyses as discussed above, the following observations on parameter influence on LOCA thermal response have been drawn.

The parameters reviewed above are listed in table 3.4 with the relative uncertainties in their magnitudes and an estimate of the incremental temperature increase associated with the uncertainty (assuming no parameter interdependence and that resulting peak rod temperatures are within coolability limits for the system). To properly evaluate the effects to be discussed ("worst case" application of the uncertainties to the LOCA induced thermal excursion), we should first review current estimates of temperature histories for power reactors. Typical examples of temperature-time histories of PWRs and BWRs are shown in figures A10.1 and A10.2 of appendix 10 as they have been calculated in accordance with IAC procedures. They show that peak temperatures during blowdown of 1700°F and 1300°F are predicted for PWRs and BWRs respectively. The maximum peak temperatures for the thermal excursion are predicted to occur during the core spray/reflood period and are estimated to be approximately 2300°F and 1800°F for the PWR and BWR respectively.

Table 3.4

Relative Influence of Selected LOCA/ECCS Parameters

<u>Parameter</u>	<u>Comments</u>	<u>Estimated Temp. Increment</u>	<u>Reference</u>
1. Initial Stored Fuel Energy/Gap Conductance	PR reduced gap coefficients h(AC) 100; h(IAC) = 500-2400	100-500°F+	(12, Table 10.6)
2. Break Flow/Transient Critical Heat Flux	BWR; -3 sec to DNB PWR; Transition Boiling period uncertainty	100°F	(12, pp. 5-5,6) (12, Table 10.6)
3. Blowdown Heat Transfer Coefficients	Uncertainty in magnitude (factor of 2)	50-400°F	(12, Table 10.6)
4. Decay Heat	Uncertainty in magnitude (5-10%)	100°F	(12, pp. 22-15,16)
5. Core Blockage (swelling & rupture)	BWR - Bundle interior blockage PWR - Varying estimates	60-520°F 26-500°F	(12, p. 20-24) (12, p. 20-18)
6. Reflood/Core Spray Heat Transfer	BWR - Analysis Model Conservation PWR - \pm 20% Data Uncertainty	200°F 100°F	(33, pp. 73-75) (12, p. 17-14)
7. Metal-Water Reactions Energy Input Embrittlement	OK - 100% Baker-Just 12% - 17% equiv. ZrO ₂ vs none (IAC)	————	
8. Reflood Rate	BWR Not rate limited Delay time critical PWR Transitional Flood Rate: 2-4 in/sec Typical rates: 1 - 1½ in/sec	$\frac{\text{Go}}{\text{No Go}}$ $\frac{\text{Go}}{\text{No Go}}$	(21, p. 4-34) (31, p. 3-27) (12, p. 14-12)

The first parameters listed in table 3.4, the initial stored fuel energy and the related gas gap conductance, are parameters which appear to have been conservatively treated under the AC revisions. However, the changes incorporated in the decreasing gas gap conductance by approximately an order of magnitude will necessitate revised temperature predictions, with anticipated increases in estimated peak temperatures of approximately 100-500°F depending upon initial power density and the exact size of gap conductance changes (assuming meltdown can be avoided). When combined with uncertainties in blowdown heat transfer coefficients, which can induce temperature increases of approximately 50-400°F by themselves, the resulting combined effect may induce temperature increments of 200-800°F, which might produce serious controllability problems in the thermal excursion.

The second item in table 3.4, the time to departure from nucleate boiling and related transition boiling relationships, bridging the transition from nucleate boiling to film boiling, have strong influences on heat transfer, especially during blowdown. The uncertainty in critical coolant flow from the break could lead to underestimation of the break flow and consequent overestimation of the duration of blowdown. For the BWRs, a conservative estimate of the influence of underestimated break flow is a decrease in the time to DNB of approximately three seconds, with a resulting increase in temperature of approximately 100°F. Though DNB for a PWR is estimated to take place in about 0.1 sec, similar uncertainties exist in PWR blowdown related heat transfer phenomena.

Combining worst case effects for the three blowdown related parameters could induce peak blowdown temperature increases of approximately 200-600°F. Thus PWR blowdown temperatures might be substantially raised above 2000°F, with consequent severe controllability problems. BWR temperatures could increase to 1500-1900°F.

Comparing the temperature increases which might accrue to the fuel rod as a result of the later time reflood/core spray heat transfer period,

it appears that additional temperature increases of 200-300°F added to peak values might be predicted as a result of combined uncertainties in decay heat, estimates of the effect of core blockage, and uncertainties in reflood core spray heat transfer mechanisms. If PWR blowdown temperatures were as high as 2000°F, from figure 3.1, reflood rates of nearly 2 in/sec would be required under normal circumstances to assure that peak temperatures would not exceed the 2200°F AC limits. With the listed additional uncertainties in energy sources and heat transfer mechanisms incurred during the reflood period, the results of table 3.3 strongly imply that the temperature turnaround might not be achievable with current ECCS designs. With substantially higher reflood rates (greater than 4 in/sec) it might be possible to override uncertainties in FLECHT heat transfer results and decay heat. As shown in figure A9.5 of appendix 9, a significant increase in initial HTC occurs in going from reflood rates of 1 in/sec to 4 to 6 in/sec. The nominal HTC at 1 in/sec is about 10 B/hr-ft²-°F and at 6 in/sec is about 40 B/hr-ft²-°F. Table 3.3 shows the benefit of increasing nominal reflood/HTC by 20 percent. A substantial improvement in controllability is achieved through this means. Increasing reflood HTCs by a factor of 4 would appear to introduce sufficient conservatism into the heat transfer processes to override many uncertainties.

It is the consummate message of many calculations of LOCA thermal excursions for both BWRs and PWRs that HTCs of less than 10 B/hr-ft²-°F are not adequate to achieve temperature turnaround. Such HTCs help to control temperature increases, fighting a holding action, but are generally not large enough to reverse the LOCA temperature gradients. The PWR-FLECHT results, figure A9.7 of appendix 9, indicate that temperature turnaround at high power levels require HTCs of at least 15-20 B/hr-ft²-°F, and that quenching of the fuel rods (the real termination of the transient associated with reestablishment of film boiling) generally occurred when HTCs reached approximately 40-50 B/hr-ft²-°F. When HTCs of this magnitude are attained, the rate of cooling is great enough that the transition to nucleate boiling takes place rapidly.

With possible PWR temperatures at the end of blowdown of the order of 2000°F (or greater) it is most important to achieve control as rapidly as possible to reduce the potential effects of metal-water reactions. Thus reflooding rates much higher than current nominal values are needed. Though the effects of core blockage are uncertain and potentially significant as shown in table 3.4, higher reflood rates will certainly aid in controlling this type of problem also. The open core of PWR permits flow diversion from local, swollen hot spots (not simulated adequately in PWR-FLECHT) which may exacerbate the thermal excursion during blowdown and the early stages of reflood. However, the same openness of the core may allow fluid to recirculate beneficially to the hot spot as the core filling process progresses -- especially given high flood rates.

Consequently, it appears that the uncertainties in LOCA parameters for PWRs make high flooding rates, of at least 4-6 in/sec, seem essential. Equivalently, heat transfer coefficients of at least 30-40 B/hr-ft²-°F are needed to assure adequate LOCA temperature control. Presently, high reflooding rates seem to be the only mechanisms by which such high HTCs can be obtained.

If BWR temperatures at blowdowns were as high as 2100°F, delay times of only about twenty seconds could be allowed between spray initiation and core reflooding to ensure that temperatures below 2200°F would be maintained, as indicated in figure 3.2. Typical delay times between core spray initiations and reflooding for current BWRs are estimated at about two and one-half minutes (60, p. 1125). A delay of two and one-half minutes implies that blowdown temperatures must be kept below 1400°F, as indicated in figure 3.2. Thus the uncertainties in BWR heat transfer mechanisms provide a basis for serious concern over the adequacy of current ECCS designs. However, reflooding rates for BWRs appear to depend only upon the capacity of the pumps provided for ECC fluid injection. There are no recognized LOCA induced flow perturbation mechanisms which might restrict desired increases in BWR reflood rates (60, p. 1092).

Since reflood delay times could be shortened by increasing the BWR flooding rates, it would appear desirable to do this to provide a clear margin of safety.

Incorporating the additional conservatisms suggested here, as partially required by the AC, would appear to necessitate increasing current BWR flood rates by approximately a factor of two in order to attain adequate safety margins. In view of the apparent uncertainties in critical parameters as listed, it would seem essential to make such an increase in flooding rate to assure conservatism.

The core flooding mechanism is obviously the most important heat transfer process in LOCA thermal excursion control for both PWRs and BWRs. When reflooding rates are sufficiently high, greater than 4 to 6 in/sec, heat transfer coefficients are apparently adequate to achieve thermal control for plants of current designs. As long as PWR blowdown temperatures can be kept below approximately 2000°F (possible with reflood rates of the order of 4-6 in/sec), it appears that it should be possible to control LOCA thermal excursions within acceptable limits. Under such conditions, costly damage to the reactor fuel rods through swelling and rupture might be expected. However, with the containment vessel presumably intact, the radiation hazard to the public would be minimal and the case for the "China syndrome" weak.

On the other hand, with current nominal PWR flood rates on the order of 1 to 1 1/2 in/sec, the ability to control the thermal excursions appears uncertain (under either IAC or AC restrictions). The results shown in table 3.3, coupled with the uncertainties listed in table 3.4, lead to this uncomfortable conclusion, under conservatively estimated conditions. The estimated two and one-half minute delay time between BWR core spray initiation and reflooding appears to be of less than adequate conservatism. Doubling the reflood rates, to approximately 6 in/sec, would help BWR margins of safety substantially. It appears that increased

flooding rates are needed for both PWRs and BWRs to assure an adequate margin of safety for LOCA thermal control. Increasing flood rates may require major redesign of current PWR ECC fluid injection methods. In view of the reduction in risks to the public to be achieved, such changes would appear to be cost effective. In the absence of flooding rates of 4-6 in/sec, essentially the only alternative for guaranteeing controllability is through reduced reactor operating power levels. In the words of the Commission:

Without redesign and backfitting, the only measures available to the operator in relation to limiting the design basis accident within the given design framework are to limit power and the power density of the reactor (60, p. 1093) (emphasis added).

Severe reductions (on the order of 40 percent) of current nuclear reactor operating power levels could be necessary to achieve unarguable levels of conservatism. The cost effectiveness of such long term power plant restrictions could be traded off against the cost of ECCS redesign for higher flood rates, but the answer seems likely to favor redesign.

3.4 Alternatives to the AC and Cost/Benefits of Their Implementation

In an environmental impact statement (EIS) (61) on the effects of the proposed AC requirements, the regulatory staff evaluated the costs and benefits of several alternatives to adoption of the AC. They find it easier to evaluate costs than benefits for the proposed action. Consequently, only costs have been estimated quantitatively to any substantial degree. The results of the study are summarized below.

3.4.1 EIS options considered

The EIS investigated six options, as follows:

1. Reaffirm the Interim Policy Statement.
2. Adopt the Proposed Rule recommended by the regulatory staff.
3. Adopt more stringent requirements and a derating of nuclear power plants beyond that recommended by the regulatory staff's Proposed Rule.

4. Adopt the recommendations of industry participants.
5. Adopt the recommendations of the Consolidated National Intervenors and the Lloyd Harbor Study Group.
6. Do not adopt any criteria; instead, evaluate each nuclear power plant on a case-by-case basis.

Each of these options, with the exception of a moratorium, would permit nuclear power reactors to be designed, built, and operated in many different modes.

To quantify the options, the staff asked the ECCS hearings participants to investigate a further subset of problems. The participants were asked to estimate the degree of plant derating required to accommodate a set of alternative conditions, which were ultimately related to temperature. The requested evaluation was for the following alternatives:

1. Criteria modified to take account of the technical conclusions set forth in the Staff Supplemental Testimony (i.e., a peak temperature of 2200°F).
2. Criteria embodying recommendations in the participant's Direct or Redirect-Rebuttal Testimony, which led in each case to temperatures greater than 2300°F.
3. Criteria limiting maximum clad temperature to 1800°F calculated with evaluation models of existing Interim Acceptance Criteria.
4. Criteria with maximum clad temperature limits so as to prevent clad swelling, with analysis done according to existing Interim Acceptance Criteria. This was dramatized by a peak LOCA temperature of 1200°F.

Responses to the regulatory staff's request were submitted by the Utilities group and Combustion Engineering. To supplement the participant results, the staff asked ANC to conduct a series of six PWR and three BWR power level sensitivity studies. The studies were parameterized in terms of linear power density and peak LOCA temperatures. Typical full power linear power densities were selected for PWRs as 17.5 Kw/ft

(on the basis of a peaking factor of 2.5); and 14.7 Kw/ft (approximately a peaking factor of 2.1). The values were selected with current regulatory requirements on reactor controllability. A peaking factor of 2.5 is the lowest value permitted for PWRs without special surveillance instrumentation. The peaking value of 2.1 is the lowest value proposed by any manufacturer. For BWRs, a peak linear power density of 18.5 Kw/ft was prescribed, corresponding to the design peaking factor for all BWRs of 2.6. For each of the specified full power levels, ANC was asked to perform LOCA analyses, and identify peak temperatures for cases where the reactors were being operated at 100, 75, and 50 percent of full power levels. The results are presented in table 3.5, as reproduced from the EIS (61).

On the basis of the results of alternative 1, the AEC concluded that the new AC specifications would cause a 5 to 10 percent derating of power plants. This they deduced could be accomplished by operating plants with normally high peaking factors, at lower values. They felt that operation of these lower, more demanding, peaking factors could be accomplished with no greater impact than "increased surveillance of existing reactor instrumentation" (61, p. 108).

The results indicated in table 3.5 indicate that if more stringent requirements (i.e., peak LOCA temperatures of 1800°F or 1200°F -- alternatives 3 or 4 respectively) are imposed, substantial reductions in plant power output would be required.

3.4.2 Estimates of "costs" of alternatives

The staff assumed that utility response to the required regulations would be to regain full power operation by replacing fuel with redesigned elements in new bundles which would have lower peak linear power ratings, but essentially equivalent volumetric power densities. That is, the volumetric power density of the reactor (Kw/ft³) would be maintained by increasing the number of fuel rods (operating at lower linear power

Table 3.5

SELECTED CALCULATIONS CONCERNING DERATING OF
NUCLEAR POWER PLANTS FOR ALTERNATIVE CRITERIA

ALTERNATIVE	PERCENT DERATING ^a				
	Utilities ^b	CE ^c	Reg: PWR, 2.5 ^d	Reg:PWR,2.1 ^e	Reg:BWR ^f
1	10	20	20	3	2
2	--	0	0	0	0
3	25-30	20	35	15	10
4	50-70	55	65	55	40

^a Derating is for a certain period of time followed by design changes permitting full power operation (see Section 3 of this chapter).

^b The ECCS Utility Group did not differentiate between PWR's and BWR's in stating their estimates.

^c CE estimates correspond to CE-designed PWR's.

^d Peaking factor of 2.5 corresponds to peak linear power density of 17.5 kw/ft at full power for the design analyzed.

^e Peaking factor of 2.1 corresponds to peak linear power density of 14.7 kw/ft at full power for the design analyzed.

^f Peaking factor of 2.6 corresponds to peak linear power density of 18.5 kw/ft at full power for the design analyzed.

levels, Kw/ft) in a given cross-sectional area of the core. Evidence that this is a probable response can already be seen on the part of reactor manufacturers who have proposed such actions (i.e., Westinghouse and General Electric).

Redesigning of fuel elements to accomplish this goal would require some time to achieve implementation. An assumed schedule for implementation was set by the staff as:

(1) The effective date of imposition of new operating limits for those plants affected by the Proposed Rule will be January 1, 1974; (2) the amount of time necessary to order and begin installing new fuel assemblies permitting full capacity operation will be 18 months (i.e., conversions beginning July 1, 1975); (3) the conversion rate to new fuel designs for those plants operating by July 1, 1975, will be linear over time and will be consummated for all plants by July 1, 1976; and (4) all new plants coming on-line after July 1, 1975, will use the new fuel technology at the outset (6, p. 109).

In order to estimate the quantitative effect of derating of the power plants to various levels, it was necessary to project the electrical generating capacity for the U.S. and the relative fraction associated with nuclear power, during the time period of interest. This was done as indicated in table 3.6 and figure 3.3.

It can be seen from table 3.6 that the impact of nuclear plant derating would not be uniformly felt across the country. The hardest hit section of the country would be the North Central: Illinois, Wisconsin, etc., where nuclear power is already a major contributor (nearly 20 percent over the period of interest) to the total electrical generating capacity for the region. The Northeast, Southeast, and the West Central are also heavily committed to nuclear power, where it will represent nearly 15 percent of the total capacity by 1975. The remainder of the country has a relatively small commitment to nuclear power of the order of 5 percent or less over most of the 2 1/2 year period of interest (1974-1976).

Table 3.6

PRESENT AND PROJECTED NUCLEAR AND TOTAL ELECTRIC GENERATING CAPACITY
FOR 8 U. S. REGIONS, 1972-1975²

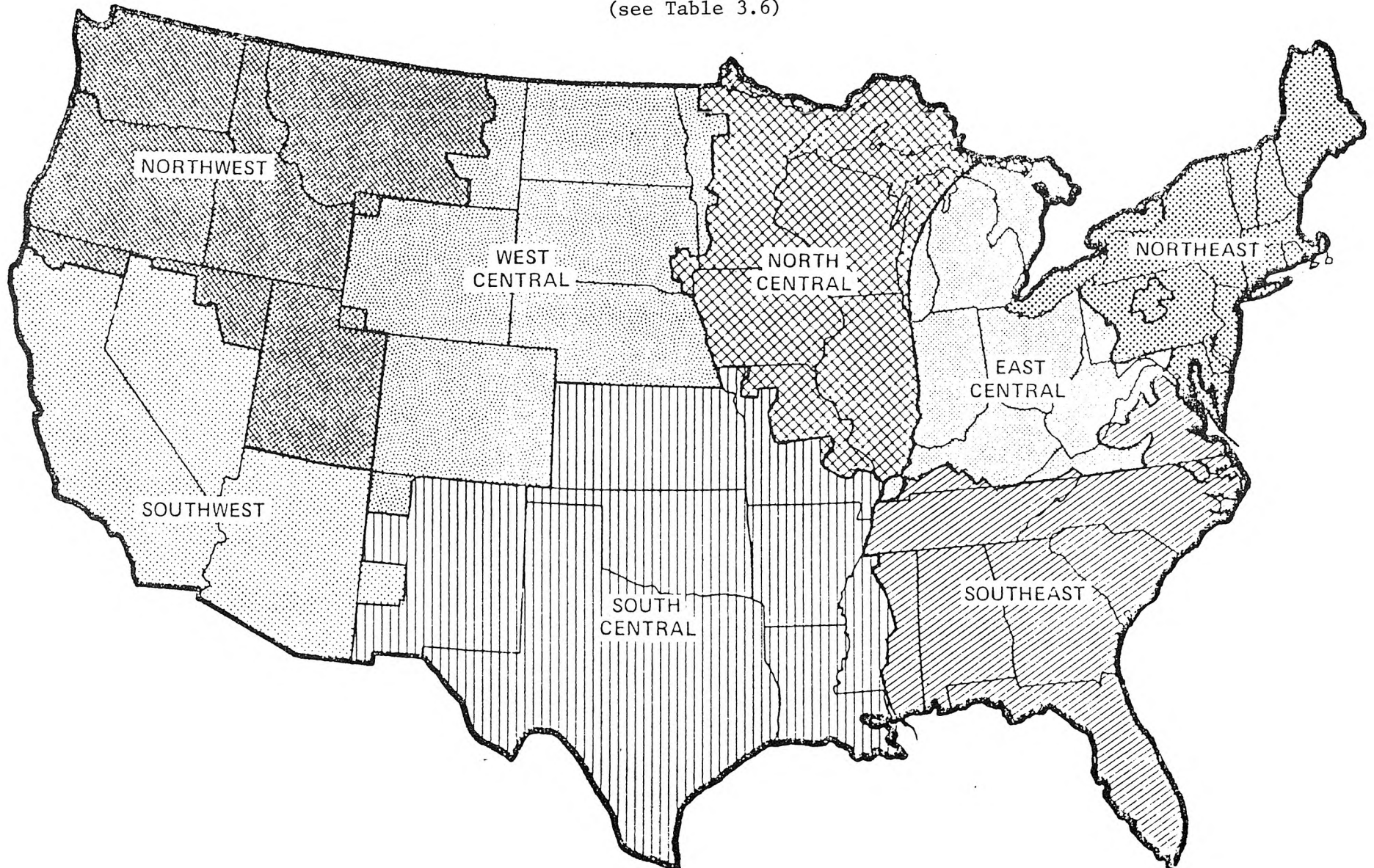
EEI REGION	1972			1973			1974			1975		
	TOTAL	NUCLEAR		TOTAL	NUCLEAR		TOTAL	NUCLEAR		TOTAL	NUCLEAR	
	MWe	MWe	%	MWe	MWe	%	MWe	MWe	%	MWe	MWe	%
NORTHEAST	75,347	4,107	5.3	87,032	8,226	9.4	97,223	11,813	12.1	102,980	15,691	15.2
EAST CENTRAL	65,853	1,041	1.6	71,500	1,041	1.5	77,769	3,043	3.9	83,163	5,021	16.0
SOUTHEAST	77,063	2,274	2.9	87,824	6,707	7.6	101,492	12,293	12.1	111,766	17,213	15.4
NORTH CENTRAL	41,700	5,019	12.0	48,254	8,304	17.2	49,776	9,428	18.9	52,731	9,428	17.9
SOUTH CENTRAL	57,343	0	0	63,990	850	1.3	68,842	850	1.2	74,667	850	1.1
WEST CENTRAL	8,995	0	0	10,735	1,565	14.6	11,379	1,565	13.7	12,017	1,565	13.0
NORTHWEST	24,772	800	3.2	26,350	800	3.0	27,540	1,930	7.0	31,044	1,930	6.2
SOUTHWEST	40,518	499	1.2	42,472	1,412	3.3	46,419	1,412	3.0	49,022	2,543	5.2
TOTALS	394,191	13,740	3.5	438,157	28,905	6.6	480,440	42,334	8.8	517,390	54,241	10.5

SOURCES: 52nd Semi-Annual Electric Power Survey, October 1972, Edison Electric Institute, pages 5-8.

Nuclear Power 1973-2000, USAEC, Wash-1139(72), December 1, 1972, pages 33-36.

²For definition of the 8 Regions, see Figure 3.3.

Figure 3.3 Edison Electric Institute Regions
(see Table 3.6)



Based upon a variety of assumptions (61, pp. 110-129), the "costs" of nuclear plant derating were estimated. Results are shown in tables 3.7 and 3.8.

The results shown indicate that the imposition of the Proposed Rule (equivalent to the AC for this study) has only about a 1 percent effect on electrical capacity and energy for the nation. However, replacement of the capacity will cost the U.S. from 200 to 400 million dollars. While the derating associated with AC imposition would have little effect on the electrical reserve margin for the country, it would cause substantial additional discharge of air pollutants. These would be the result of additional coal and oil burning to substitute for the unavailable nuclear power.

However, the impact of the AC is relatively light compared with the alternatives considered. In the extreme case of a nuclear moratorium, approximately 9 percent of the U.S. electrical capacity would be lost along with a loss of as much as 14 percent of the total energy. Replacement costs for the lost capacity and energy would be a factor of 10 greater than costs for imposition of the AC. In the case of a moratorium, the electrical reserve margin could be reduced to an unpleasantly small value of nearly 10 percent, while air pollutants would be increased by a factor of 10 above the case for AC imposition. The costs of other concepts are proportionally distributed in accordance with relative changes in derating requirements.

In addition to the costs of temporary replacement of electrical energy and capacity with fossil fueled power plants, modifications to the nuclear reactors to permit them to regain their original power levels would require substantial capital investments. On an individual reactor basis, each 1000 MW_e reactor would require from 3.5 to 10 million dollars of additional capitalization to modify and replace reactor fuel elements with acceptable designs.

Table 3.7 (from 61)

REPLACEMENT CAPACITY AND ENERGY REQUIRED BY RULE MAKING ALTERNATIVES

	I Interim Policy Statement	II Proposed Rule	III Further Deratings		IV Moratorium ^a
<u>Effect of Derating</u>					
1.1 Derating (Percent)	0	5-10	25-30	50-70	100
1.2 Reduction in Capacity (thousand MWe)					
1974	0	2.11-4.23	10.6-12.7	21.2-29.6	42.3
1975	0	2.37-4.75	11.9-14.2	23.7-35.0	47.5
1976	0	0.33-0.68	1.8- 2.1	3.9- 5.7	6.8
1.3 Percent of Total Capacity Affected					
1974	0	0.4 -0.9	2.2- 2.6	4.4- 6.2	8.8
1975	0	0.4 -0.9	2.3- 2.7	4.5- 6.7	9.2
1976	0	0.05-0.1	0.3- 0.4	0.7- 1.0	1.2
1.4 Energy Affected (billion KWH)					
1974	0	13.3 -26.7	66.8-80	133-187	267
1975	0	15.0 -30.0	74.8-89.2	150-210	299
1976	0	2.1 - 4.3	11.2-13.4	24- 34	43
TOTAL	0	30.4 -61.0	153-183	307-431	609
1.5 Percent of Total Energy Affected					
1974	0	0.6 - 1.3	3.3- 4.0	6.6- 9.3	13.2
1975	0	0.7 - 1.4	3.4- 4.1	6.9- 9.7	13.8
1976	0	0.1 - 0.2	0.5- 0.6	1.0- 1.5	1.8

^a Recommendation of CNI and Lloyd Harbor Study Group

Table 3.8
Cost Comparison of ECCS Rule Making Alternatives

RULE MAKING ALTERNATIVES					
	I Interim Policy Statement of 1971	II Proposed Rule	III Further Deratings		IV Moratorium ^c
1. Assumed Derating (Percent)	0	5-10%	25-30%	50-70%	100%
2. Capacity & Energy Penalty (millions of dollars/year)					
a. 1974	Base	\$ 84-169	\$422- 507	\$850-1,180	\$1,690
b. 1975	Base	95-190	474- 570	950-1,330	1,900
c. 1976	Base	14- 27	71- 85	150- 230	270
d. Total	Base	\$193-386	\$967-1,162	\$1,950-2,740	\$3,860
3. Reliability of Electrical System					
a. National Reserve Margin, 1975 (in %)	25.0	24.3-23.7	21.0-21.7	15.6-18.3	11.6
4. Chemical Discharges to Ambient Air (thousands of tons)					
a. Particulates	Base	16-32	84- 100	180- 260	320
b. Sulfur Dioxide	Base	170-340	900-1,070	1,940-2,730	3,400
c. Nitrogen Oxides	Base	90-180	480- 570	1,030-1,450	1,800
5. Modifications to Fuel and Reactor (100 MWe Unit) ^d					
a. Capitalized Cost of Modifications ^a	Nil	\$13.3/kW	\$13.3/kW	\$13.3/kW	--
b. Capital Investment (million dollars)	Nil	\$ 3.5-10	\$ 3.5-10	\$ 3.5-10	--
c. Increase Fuel Costs (thousand dollars/yr)	Nil	\$ 215-520	\$215-520	215-520	--
d. Effectiveness of Modi- fications (Final Power Output in %)	Nil	100	85	60	--
e. Replacement Power Costs (million dollars/yr)	Nil	0	\$6	\$16	--

^aTo the extent that corrective action with new fuel designs has already been initiated as a result of the Staff's Supplemental Testimony filed in October 1972, and also as a result of the Concluding Statement being filed in advance of the effective date of the Proposed Rule, the delay in achieving conformance with the Proposed Rule might be shortened and these costs thereby modified. These figures would also be modified if the Implementation period were longer than presently proposed.

^bThis represents the equivalent capitalized cost including both capital investment (5b) and fuel cost increases (5c) associated with fuel modifications.

^cRecommendation of CNI and Lloyd Harbor Study Group

^dThis data on modifications to fuel and reactor was provided by the ECCS Utility Group and has not been independently verified by the AEC Regulatory staff (see text).

3.4.3 Estimated "benefits" from alternatives

Though the regulatory staff was willing to make a sufficient number of assumptions in the EIS to quantify "costs," they were unwilling (or unable) to make similar decisions regarding relationships affecting the benefits of various alternatives. Consequently, benefits were described only in qualitative terms. In the words of the staff:

Lacking accurate numerical values for the probabilities associated with various amounts of derating, we cannot quantify the "benefits" and hence compare them to the costs. Instead, we must use the technical judgment exemplified by the staff Concluding Statement and the technical portions of the record of the rule making proceedings (61, p. 142) (emphasis added).

This led to presentation of largely visceral statements about "conservatism," as benefits, associated with various alternatives. The relative benefits of adoption of the AC (essentially equivalent to the referenced "Proposed Rule") are described as follows:

The significant difference between the Proposed Rule and the requirements of the Interim Policy Statement are given in Section I.C. of the staff Concluding Statement. In general, the changes have been in the direction of increased realism in the calculation, by taking into account phenomena that were neglected or approximated in the earlier evaluation models. The criteria changes are in the direction of increased conservatism. This combination of increased conservatism results, in the staff's opinion, in an improvement in the new criteria and models over the old. That is why the staff has recommended orderly implementation of the Proposed Rule.

The implication is clear that the improvement in the Proposed Rule (AC) over the Interim Policy Statement (IAC) gives rise to a larger margin of conservatism and a higher probability, in the sense previously discussed, that the criteria and models are adequate. The staff believes this to be true. For the reasons discussed previously, no numerical value can presently be placed accurately on this improvement in probability and margins. It seems evident to the staff, however, that it is significant in the present state of our knowledge. If this is true, then the Interim Policy Statement rules necessarily have a lower probability than the Proposed Rule that the criteria and models are adequate -- thus a lower margin of conservatism (61, pp. 140-141) (emphasis added).

The benefits of derating nuclear plants beyond AC requirements were belittled, as bringing negligible benefits, as follows:

Further derating of nuclear power plants beyond that inherent in the Proposed Rule would result in more conservative plant operation and thus, in principle, greater margins and higher values of the probability that the criteria and evaluation models (if such there are) have been correctly chosen.

But this is illusory; any possible increase in the margins and the probability over and above the Proposed Rule is believed by the staff to be negligible. Therefore, the increased cost of such derating [see section 5 of this chapter] would be compensated in this case by a negligible benefit (61, p. 142).

On the other hand, the vendor recommendations for no derating were implied to probably be basically correct (but unjustified on the basis of present knowledge), as follows:

The recommendations of the Industry Participants are in every case less conservative than the Proposed Rule and, in addition, all industry recommendations except GE's are less conservative than the Interim Policy Statement.

In fact, to make up for gaps in present knowledge, the staff has chosen in the Proposed Rule an alternative that is very likely more conservative than would be justified if knowledge were more complete. Thus, the unanimous recommendation of the four reactor vendors and the Consolidated Utilities -- that the Interim Policy Statement is at least conservative enough -- may be true. However, in the present state of knowledge, the staff believes that the enhancement of public health and safety justifies implementation, in an orderly way, of the improvements and increased conservatism of the Proposed Rule. To go further, in the staff's opinion, would only be to increase a probability already adequate and to decrease a risk already negligible (61, p. 143) (emphasis added).

The benefits of the moratorium were described as:

A moratorium on nuclear power plant licensing would reduce the risk from nuclear power plants essentially to zero. This risk.....is already very low. Therefore, the moratorium, while it would theoretically minimize this risk and maximize the "benefits" of the ECCS rule making, it even less necessary and vastly more costly than extensive derating as discussed.....above.

The need for such a choice could only be justified if presently available experimental and analytical information were not sufficient to support the conclusion that the Proposed Rule or one of the more restrictive alternatives.....were adequate to protect the public health and safety and the environment. If such were the case, a moratorium would have to remain in effect for the period of time necessary for ongoing research programs to confirm certain engineering assumptions and numerical values used by the Regulatory Staff in evaluating ECCS performance. The Regulatory Staff does not agree that there is insufficient information available upon which to judge the effectiveness of ECCS performance and believes that the ECCS hearing record supports it in this regard. Apart from the lack of technical justification for a moratorium, such a course would impose severe health, economic, and environmental penalties (as discussed elsewhere in this chapter) out of proportion with the supposed risk which a moratorium would be designed to avoid (61, p. 144) (emphasis added).

Finally, the concept of having no general criteria was dismissed as being of no real influence on the real world. In the opinion of the regulatory staff, the hearings results would influence case-by-case decisions made even if there were no specifically written criteria. Judgements would still be based upon the same information and results would eventually be identical -- although the regulatory plant reviews would probably be more painful in the absence of a set of definitive criteria.

In summary, the cost of imposition of the AC was given as a 5-10 percent derating of nuclear power plants. This derating would cost utilities (and ultimately the public) about 200 to 400 million dollars in replacement capacity and energy costs. The costs of modifications to the nuclear fuel assemblies were estimated to cost 3.5 to 10 million dollars per 1000 MW_e of nuclear power. [This would be in addition to the 200-400 million dollars required for the temporary replacement of lost energy and capacity.] Costs of other alternatives were as much as a factor of 10 higher, as in the case of a complete moratorium. In addition, there are other "costs," social and economic, which vary from region to region

within the U. S., resulting from increased probabilities of power outages and higher air pollution caused by increased use of coal and oil burning power plants.

Because no good statistical basis exists for quantitatively evaluating the margins of safety associated with any of the alternatives, benefits were presented in a purely qualitative fashion. This is a serious shortcoming of the EIS. Without a quantitative presentation of benefits, it is difficult to adequately compare costs and benefits for the various alternatives. This problem will continue to plague the nuclear industry until a good statistical base is obtained for such analyses.

An initial study of the probability and consequences of nuclear accidents has been completed by Professor Rasmussen of MIT, for the AEC (67). It represents a valuable source of quantification of problems of this sort. However, it must be recognized that the statistical base for LOCA analyses is essentially nonexistent. Consequently, though of real interest and benefit to everyone (from AEC to intervenors) the Rasmussen study depends heavily upon technical judgement and must not be expected to be the final answer to "benefit" quantification.

4.0 CONCLUSIONS

An interesting summary of the ECCS hearings has been published by Cottrell, as follows:

Review of the ECCS Rule-Making Hearing and its ramifications leads the author to the conclusion that much good has resulted from this unique experience. This "good" falls into several categories:

1. The recommended modifications to the ECCS criteria have enhanced the safety of nuclear power reactors to a level that satisfies most technical experts.
2. The hearing forced a new look at reactor safety research, i.e., what was being done and by whom.
3. Organizational changes have been induced in both the AEC and contract organizations which are intended to expedite the conduct and evaluation of needed research.
4. Several administrative problems were brought to light. Some of these have been resolved, while others are still under study (e.g., the availability of information on government-sponsored work, questions of proprietary safety information, and procedures for the promulgation of criteria).

However, the price paid for these gains was high. Not only was the hearing itself a traumatic experience for all concerned (organizationally as well as individually), it was also very expensive. Additional costs will be reflected in the deratings and/or changes in existing plants, as well as costs for new designs that the vendors are now developing. But these are transient costs and, in the final analysis, are the costs for developing safe nuclear power reactors. It is fortunate, considering the energy demands of our technological society, that these additional costs will not have a major impact on the costs of nuclear energy, so that it remains a viable option for the near future (i.e., 25 to 50 years).

Measured by almost any criterion, the ECCS intervenors have won a major victory. They have been instrumental in causing the AEC Regulatory Staff to recommend ECCS criteria that are significantly more conservative (i.e., safer) than the June 1971 criteria. In fact, in backing off to these new criteria, the AEC has accommodated the reservation of most of those in the nuclear community who previously expressed concerns about the adequacy of the 1971 criteria. Despite this accommodation, the principal intervenors, CNI, continue to oppose the Commission, as indicated most recently by their collaboration with the Friends of the Earth and Ralph Nader in the suit against the AEC. However, with the majority

of the scientists and engineers in our technical community concurring regarding the adequacy of the conservatism of the Regulatory Staff's concluding statement, it seems unlikely that the intervenors can effect any further significant change at this time.

In the final analysis, the hearing was a rough way to go, but it was a viable route and one which produced many beneficial results (62, p. 53).

4.1 Results of ECCS Hearings

Adversary hearings are indeed a "rough way to go"! The ECCS adversary hearings seemed to place intervenors, who had essentially no actual reactor design experience, at diametric odds with the much more experienced AEC and vendor representatives. Since each side saw the other in the role of an adversary, neither seemed willing to freely and openly discuss the technical issues. As a result, the hearings were unable to completely bridge the gulf of differences between the two parties. As Cottrell noted, the principal intervenors, the CNI, continue to express their concern over reactor safety in spite of the accommodation of many of their original reservations within the AC.

It is interesting to analyze the dimensions of the gulf separating the two camps in terms of the self-images of the opponents and the arguments which they have presented. The vendors and the AEC expressed the feeling that the ECCS design, based upon sound engineering practice (which has been applied for generations in related non-nuclear problems of heat transfer, pressure vessel and piping design) had assured reliability for operation in the event of a LOCA. As evidence of this, they pointed to the multiple safety barriers built into the system, the "defense-in-depth" concept of three levels of defense: (1) quality assurance in design, fabrication, and operation; (2) redundant systems elements, periodic in-service testing, etc; and (3) the installed engineered safety systems such as the ECCS. (These last are designed to "mitigate the consequences of postulated serious accidents" no matter how small the probability of such accidents might be.)

The vendors acknowledged that they do not understand the LOCA/ECCS problem completely. But they feel that uncertainty with respect to some aspects of systems response is common to the engineering design of most large systems, independent of whether the system deals with nuclear power plants or aircraft or automobiles. The engineer is pragmatic. Recognizing that he rarely has absolute and complete knowledge and understanding of all aspects of a problem, he feels it is only necessary to have bounded the response of the system within reasonable conservative limits by his design. With a system as complex as the ECCS, he feels that the supply of physically interesting, challenging, and unresolved problems is essentially unbounded -- and their investigation could go on forever. Thus the vendor feels that total understanding of all LOCA/ECCS problems is not necessary as long as system performance is reasonable and conservatively assured.

In this environment, the intervenors were at a great disadvantage. They were generally not completely familiar with ECCS engineering design details and specifically not totally familiar with all the systems studies which have been performed for the ECCS, many of which may never have been openly published by the vendors, some of the latter having been considered as inconsequential or perhaps having negative results. Consequently, to lend support to their position, the intervenors amassed as great a collection of "expert" opinion showing dissent over the engineering practice of the vendors as possible. The intervenors have argued that the existence of such dissent demonstrates the unreliability of the system. Their principal goal appears to have been to find a sufficient number of acknowledged problems, where experts had expressed differences of opinion, to make the balance of evidence appear uncertain to "reasonable men." The intervenors have felt it necessary only to establish reasonable doubt about ECCS reliability. They argue that since clearly the responsibility for proven conservatism, in the face of uncertainty, lies with the AEC and the vendors, the ECCS must be considered basically ineffectual until

proven reliable. Moreover, in the intervenors' opinion, the magnitude of the consequence of a LOCA in which the ECCS did not perform adequately, no matter how small the probability of the LOCA, makes it imperative that all problems and areas of uncertainty in ECCS design should be resolved before nuclear reactors can be considered "safe" and worthy of extensive utilization.

In the final analysis, both sides have been guilty of allowing external parameters of the problem to influence their judgment. In fact it is the opposing views of the ECCS externalities which have established the dimensions of the gulf separating the adversaries and served to maintain the division in spite of the evidence presented by both sides at the hearings. On the part of the AEC and the vendors, their view of the low probability of a LOCA has reduced their concern over the uncertainties in the physics of the ECCS design. On the other hand, the intervenors' perception of the magnitude of the consequences of ECCS failure in event of a LOCA clouds their ability to objectively evaluate the issues which they have raised. To the public, finding the common ground -- or solution -- between the two extreme positions, seems like the classical problem of the product of zero and infinity, an indeterminate form for which the solution is uncertain.

4.2 Evaluation of Criteria "Uncertainties"

Whatever final conclusion is reached about the ultimate adequacy of the AC, it must be acknowledged that it is substantially more specific and conservative than the IAC. The AC is much more complete in its specification of the details of the ECCS. A serious attempt was apparently made to provide specifications which satisfied a consensus of technical opinion and eliminated several areas of ambiguous or non-existent treatment of elements of the ECCS design.

In the AC preparation, the Commission seems to have given serious consideration to the issues raised in the hearings. In their discussion of the specific elements of the AC, the Commission treated in varying

degrees most of the questions and comments raised by all participants: intervenors, vendors, and consultants. They appear, in particular, to have been influenced by the ACRS response to the intervenor interrogatories. The AC elements have dealt (in varying detail) with essentially all the ACRS list of uncertain conservatisms, plus some ECCS design issues which were not included in the list.

As described by the Commission:

The principal changes from the Interim Policy Statement are as follows. The old criterion number one, specifying that the temperature of the zircaloy cladding should not exceed 2300°F, is replaced by two criteria, lowering the allowed peak zircaloy temperature to 2200°F and providing a limit on the maximum allowed local oxidation. The other three criteria of the IAC are retained, with some modification of the wording. These three criteria limit the hydrogen generation from metal-water reactions, require maintenance of a coolable core geometry, and provide for long-term cooling of the quenched core.

The most important effect of the changes in the required features of the evaluation models is that swelling and bursting of the cladding must now be taken into consideration when they are calculated to occur, and that the maximum temperature and oxidation criteria must be applied to the region of clad swelling or bursting when the maximum temperature and oxidation are calculated to occur there. Another important change is the requirement that, in the steady state operation just before the accident, the thermal conductance of the gap between the fuel pellets and the cladding should be calculated taking into consideration any increase in gap dimensions resulting from such phenomena as fuel densification, and should also consider the effects of the presence of fission gases. When these effects are taken into consideration a higher stored energy may be calculated. Other changes in the evaluation models are mostly in the direction of replacing previous broad conservative assumptions with more detailed calculations where new experimental information is available or where better calculational methods have been developed (60, p. 1093) (emphasis added).

Some of the responses in the AC to the ACRS listed problems seem to have been adequate to have achieved the desired conservatism sought by

the ACRS. For example, in their treatment of the initial stored energy of the fuel, the Commission has considered the influence of clad swelling and rupture on gas gap conductance in what can be a satisfactorily conservative manner, assuming the "case-by-case" follow-up required during licensing procedures is adequate.

What, then, are the most serious remaining problems with ECCS design and criteria? A more detailed discussion of the parameters and their relative importance has been given in chapter 3, especially section 3.3 (supported by several appendices). The single most important ECCS parameter for both PWRs and BWRs appears to be the reflood rate. Current estimates of PWR reflood rates range from less than one inch per second to two inches per second. At these reflood rates, using adequately conservative values of critical thermal parameters (table 3.4), predicted LOCA thermal excursion controllability is uncertain for PWRs of current design. Although predicted BWR flooding rates are substantially higher than PWR rates (nearly 4 in/sec), the long delay time (nearly 3 minutes) between core dryout and the beginning of reflooding (when the ECC fluid first refills the pressure vessel to the bottom of the core) is very hazardous. Again, using adequately conservative values of critical thermal parameters (table 3.4), delay times prior to reflooding should be restricted to a period of from one to two minutes (at the longest). Consequently, one might conclude that for BWRs, as well as for PWRs, thermal excursion controllability is uncertain at current design operating conditions and flooding rates.

The problem with current ECCS design can be resolved into three main categories: (1) uncertainties in LOCA energy sources; (2) blowdown-related uncertainties; and (3) core reflooding related uncertainties. A succinct list of individual parameters at issue within these categories is given in table 3.4, including measures of the relative importance of each parameter. The reader is referred to this table and its supporting discussion, for more detailed discussions of the parameters.

With respect to energy source uncertainties, the AC has specified criteria which appear adequately conservative with respect to the initial stored fuel energy of the rod. It should be observed that the AC specifications are more conservative than those of the IAC. Consequently, the revised criteria will result in increases in estimated peak fuel rod temperatures of about 100-500°F above those previously calculated under the IAC guidelines. On the other hand, an uncertainty of 10-15 percent exists in the conservatism of the specification of decay heat for the fuel rod. This uncertainty might contribute an increase in peak temperatures estimates of about 100°F above current IAC predictions. With respect to specification of energy release from metal-water reactions (zirconium - steam), the use of the full Baker-Just relationship, as prescribed in both the IAC and AC, appears to be adequately conservative. Though this energy source may be critically important to LOCA thermal excursions, it should not contribute an unanticipated source of temperature increase above earlier IAC predictions.

In the area of blowdown-related uncertainties, problems exist from several parameters; specifically: the specification of magnitude and duration of critical flow from broken pipes; critical heat flux related parameters; and the magnitude of blowdown heat transfer coefficients, as well as the effects of core blockage due to swelling and rupture of the fuel rods which (for PWRs) would most probably take place during blowdown (if it occurs at all). The combined effect of these parameters, including the increased conservatisms in treatment of initial stored fuel energy of the AC, might induce as much as a 200-600°F increase (over the IAC predictions) in the fuel cladding temperature during blowdown.

Core reflooding-related uncertainties include problems in the areas of: the adequacy of treatment of reflood/core spray heat transfer experimental data; the effects of core blockage from swollen and ruptured rods during the reflood period; and the conservatism of AC prescriptions of

metal-water induced clad embrittlement. Though the embrittlement problem does not contribute to a specific peak clad temperature differential, it does raise concern over brittle failure of rods during quenching. Such failures could result in exacerbation of local blockage with resulting impairment of hot spot cooling. Aside from the embrittlement phenomena, the net temperature increase expected from reflood-related uncertainties (including the effect of decay heat uncertainties) might be as high as 200-500°F above predictions from the IAC. In evaluating the credibility associated with the author's estimates of the uncertainty in blowdown and reflood temperature increments, as given above, it is well to bear in mind that they are the result of the technical judgement of the author (who accepts responsibility for them). As previously discussed (section 4.4) "technical judgement" admits a wide variety of conclusions with respect to margins of safety -- which can, after appropriate liberties are taken, be translated into temperature increments (or other quantitative measures, as desired). It may be well to reemphasize this point with the words of Cottrell:

The whole purpose of the hearing was to determine the adequacy of the June 1971 IAC as the basis for licensing reactors. No one, neither the Commission, the vendors, nor the intervenors, has a good quantitative basis for determining whether any given reactor is safe or unsafe. All parties depend on the judgment of their experts in arriving at this decision. Since the AEC Regulatory Staff has been working most closely with the vendors over the years, it is perhaps reasonable to expect a greater understanding of this elusive judgment between these groups than between any others. However, considering the magnitude of the commitment to nuclear power, the importance of safety, and the need for public understanding, it is unfortunate that there has been so little effort expended in attempting to elevate nuclear risk assessment to a more exact science. However, in August 1972 the AEC embarked upon a major project toward this end. The Reactor Safety Study, also known as the Rasmussen Study (for Dr. N. C. Rasmussen of MIT who heads this project), is now in full swing, and a report is scheduled for 1974. Although this report is unlikely to be a panacea for the safety-evaluation business, it will be the first step down a long road (62, pp. 51,52) (emphasis added).

Though this is not intended as an apology by the author for exercising his own judgement, it is indeed unfortunate that no "good quantitative basis" exists to permit adequate definition of the margin of safety.

With these provisos on technical judgements in mind, we need to put the potential temperature increases (as suggested above) into the proper perspective. The following estimates of critical reactor temperatures have been made by the vendors for their own reactors on the basis of IAC requirements (see figures A10.1 and A10.2 of appendix 10). Typical estimates of blowdown temperatures for PWRs are approximately 1700°F, while predicted BWR temperatures reach only about 1300°F during blowdown. Maximum temperatures for both PWRs and BWRs occur during reflood/core spray periods and are estimated respectively as 2300°F and 1900°F. It should be noted that estimated peak temperatures for PWRs using the IAC may already frequently exceed the AC limit of 2200°F, while the BWR had a cushion of about 300°F under the less conservative requirements of the IAC. Simply applying the AC requirements (without modification) would make ECCS performance margins for both PWRs and BWRs uncertain. The effects of the increases in conservatism described above, which sometimes exceed AC specifications, will be considered below.

The critical question is: what are the implications of the uncertainties in the conservatism of IAC and AC specification of the critical parameters? From the blowdown-related uncertainties with potential temperature increments of 200-600°F, it can be inferred that maximum blowdown temperatures for PWRs might be predicted to reach (or exceed) 2000°F. At these temperatures, immediate post-blowdown control is critical.

With current PWR design practice, for which nominal reflood rates are approximately 1-1/2 in/sec and peak linear rod power density is about 19 Kw/ft, controllability of the thermal excursion is uncertain if peak blowdown temperatures reach 2000°F. (Compare results of figure 3.1 and table 3.3.) The question can be legitimately raised as to whether or not

it is possible to assure controllability under these conditions. The PWR-FLECHT results (see fig. A9.5) provide an important input to the answer to the question! The results show that for fuel rod initial conditions of 2000°F and an equivalent linear power density of about 18 Kw/ft, if flooding rates are 6 in/sec or greater, clad temperature increases are limited to 100°F (or less). In the tests, temperature turnaround times were of the order of 10 seconds or less and typical quench times (when nucleate boiling was regained for the rod) were approximately 75 seconds.

Thus it appears that the key to PWR thermal excursion controllability in a LOCA is obtainable through sufficiently high flooding rates. In fact, it appears that if flooding rates equal to or greater than 6 in/sec can be assured for the reactor, sufficient coolability would be provided to overcome the uncertainties associated with the specifications of the re-flood period parameters.

A flooding rate of 6 in/sec produces an initial nominal reflood heat transfer coefficient (HTC) of approximately $40 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$, for about 4 times the magnitude of the nominal HTC at 1 in/sec (approximately $10 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$). This factor clearly dwarfs a 20 percent uncertainty in the specification of the 1 in/sec reflood HTC. Similarly the factor of four increase in the HTC associated with the 6 in/sec reflood rates overrides the 10-15 percent uncertainty in definition of decay heat for the fuel rod, while the rapid quenching assures dissipation of the fuel rod heat without significant problems. Additionally, the rapid temperature turnaround time and short time to quenching substantially reduce the probability that the more conservative limits recommended for rod oxidation, to prevent embrittlement, would be exceeded. Moreover, though the effect of core blockage from swollen and ruptured rods on flow diversion from local hot spots is highly uncertain (PWR-FLECHT tests of this problem simulated actual PWR core conditions very poorly), higher flooding rates must surely improve heat transfer, even under these uncertain conditions.

In analyzing BWR performance in a LOCA, it appears that they have two significant advantages over a PWR. First, the nucleate boiling period during blowdown is substantially extended in a BWR, compared to the equivalent period in a PWR. The estimated time to departure from nucleate boiling in a BWR is about from 5 to 10 seconds, compared to an equivalent estimate of about 0.1 second in a PWR (60, p. 1116). For every second that the departure from nucleate boiling can be postponed in a reactor, the initial stored energy can be dissipated at the equivalent rate of from 40 to 150°F/sec. This effect is one of the principal sources of the large difference in maximum blowdown temperatures for the two types of reactors (1300°F for BWRs instead of 1700°F predicted for PWRs under IAC rules).

The second beneficial aspect of BWR design is that there are no recognized mechanisms leading to flow restrictions which would limit reflood rates through steam binding. Consequently, BWR reflood rates apparently depend simply upon the capacity of the reflood subsystem pumps. Thus, there is no apparent inherent reason why BWR flood rates could not be increased, as needed.

In balance, because of the inherent differences in operational characteristics and reactor dimensions of BWRs and PWRs, approximately four times as much water must be added to the BWR, as compared to an equivalent PWR, to initiate reflooding. The effect of this is apparent in the BWR delay time of approximately 2-1/2 min between the beginning of the core heatup period and reflood (60, p. 1125). Thus the inertia of a BWR to reversal of the LOCA thermal excursion would be greater than that of PWR under equivalent fluid input conditions. Therefore, since time delays might be expected to be greater for BWRs than PWR, for equivalent reflooding rates, it is fortunate that the steam binding and CHF problems are simpler for the BWR.

Considering the BWR LOCA thermal excursion, the additional uncertainties previously discussed with respect to blowdown-related parameters might increase maximum temperatures during this period to as high as 1900°F. With core spray HTC specified as ranging from 1.5 to 3.5 B/hr-ft²-°F (AC, Sec. I.D.6) the core spray is generally inadequate to reverse the temperature transient (though it provides important temporary control prior to reflood) (60, p. 1125). The AC specifies reflood HTC values of 25 B/hr-ft²-°F (AC, Sec. I.D.6), which is reported to be associated with reflooding rate of 3.7 in/sec (60, p. 1125). From the PWR-FLECHT reflood data, a reflooding HTC of 25 B/hr-ft²-°F corresponds to a nominal HRC for flooding rates of about 2-3 in/sec. Since the PWR-FLECHT data (figure A9.7) indicates that reflooding rates of the order of 4 in/sec would have somewhat higher HTC values (on the order of 30 B/hr-ft²-°F), it appears that AC prescribed BWR reflooding HTCs are somewhat conservative. On the basis of LOCA parameters specified by the IAC, reflooding, with the specified 25 B/hr-ft²-°F HTC, has been calculated to achieve temperature turnaround promptly for all cases bounded by IAC limits. Peak temperatures attained during the transient are directly related to the time between spray initiation and core reflooding (figure 3.2).

For maximum blowdown temperatures of 1300°F (as calculated under IAC specifications), approximately a three minute delay between core spray initiation and core reflooding would be allowable before peak temperatures would reach the 2300°F IAC limit (figure 3.2). As previously noted, for current BWR design reflood rates, core reflooding is predicted to occur approximately two and one-half minutes after spray initiation, allowing a relatively comfortable margin of safety within IAC specifications. A maximum temperature of about 1500°F at the end of blowdown would be tolerable (figure 3.2). However, using the conservative LOCA parameter estimates of this review, revised estimates of blowdown temperatures are obtained of as high as 1900°F. At these temperatures, a delay time margin of only about 60 seconds exists before temperatures reach critical AC

limiting values of approximately 2200°F. With the additional parameter conservatisms estimated to be required during the reflood period (under the assumptions of this review), the delay time must be kept to an absolute minimum to keep temperatures within bounds of controllability.

If the same margin of reflooding rate safety were to be maintained under the more conservative parameter assumptions discussed previously, an increase in the current BWR flooding rate of approximately a factor of two would be required. Reducing the spray initiation-to-core reflooding delay time to approximately one minute would be equivalent to increasing the flooding rate to about 9-10 in/sec, with a corresponding increase in HTC to approximately 50 B/hr-ft²-°F. Thus, increasing the flooding rate decreases the delay time and has an extra compensation of increasing the flooding heat transfer coefficient as well. These complementary changes would apparently provide acceptable safety margins even within the more conservative assumptions reviewed here.

4.3 Alternative Courses of Action

As observed earlier in the section, substantial and apparently irreducible differences of opinion exist with respect to ECCS operational reliability, even among experts in the field. Though certain observations have been made in this report in connection with criteria conservatisms, it has been impossible to resolve in absolute terms which parties have the balance of "truth" upon their side. Consequently, a number of alternate methods of resolving the issues of uncertainties suggest themselves.

Though a spectrum of alternatives are possible, ranging from acceptance of current procedures and designs to a total ban on the use of LWRs, certain steps seem more reasonable than others. The intervenors have essentially recommended foreclosure of current and future light water power plant operation and construction -- a course which would result in substantial local hardship through power shortages in certain portions

of the U.S., especially in this period of general weakness in the availability of energy supplies. Derating operational power plants to peak linear rod power densities substantially below current limits (reductions of as much as 40 percent) have also been suggested. In this regard, the Commission has suggested:

Without redesign and back-fitting, the only measures available to the operator in relation to limiting the design basis accident within the given design framework are to limit the power and the power density of the reactor. The power density can be manipulated somewhat independently of the total reactor power by adjustments of fuel enrichment and control rod action to provide more uniform power generation throughout the core. The Commission notes that there has been a tendency to reduce the maximum allowed peaking factor (ratio of the highest power density to the average throughout the core) to satisfy ECCS criteria. These lower allowed peaking factors leave less margin above the normal operating range for maneuvering; thus greater care in reactor operation is required to ensure that these factors are not exceeded (60, p. 1093) (emphasis added).

Thus even the concept of power density limitation is not without certain attendant operating problems. Even if such problems are minimized, however, this approach would probably also produce local power shortages. Another course might be to delay licensing of new LWR construction while results of a substantially increased and accelerated research program were obtained, analyzed, and incorporated into subsequently revised criteria. Alternatively, the AEC might attempt to develop design criteria which were accepted by all parties as clearly conservative which could be imposed as engineering standards. This might lead to redesign of the ECCS to assure that its reliability is adequate to satisfy the concerns of all parties. Of course, the AEC could attempt to continue operation using their current criteria, with the strong probability that future construction may be delayed by intervenor-induced court proceedings. The possibility of locally legislated moratoriums on nuclear power plant construction is not unlikely. (Activity is underway to introduce such a referendum for submission to the voters of California.)

Combinations of the above steps could also be considered. For example, restricted or delayed licensing of nuclear construction, combined with some plant derating, might be considered for individual utility power networks. A risk-benefit analysis of such steps could be conducted for the local and regional areas affected by the actions under consideration. Decisions on whether to accept the risks of continued operation or the potential costs/benefits of the restrictions could then conceivably be decided upon a case-by-case basis.

The ECCS Environmental Impact Statement presented an evaluation, of sorts, of the costs and benefits for several alternative methods of resolving the ECCS problem. Though no quantitative relationships for benefits were given, and currently no reliable estimates could be given, estimates of the costs of several alternatives were presented. For example, imposition of the AC was estimated to require derating of currently operational nuclear power plants by 5-10 percent for a period of up to 2-1/2 years (through 1976). Replacement electrical capacity and energy was estimated to cost 200 to 400 million dollars, plus the capital investment for retrofitting the reactor with redesigned fuel elements (3.5-10 million dollars/1000 MW_e plant) to remove the requirement for plant derating. Derating would also cause additional social and economic cost penalties, directly and indirectly, as a result of increased probabilities of power outages and higher air pollution (caused by increased use of coal and oil burning plants). Several alternatives were considered, including a complete moratorium on nuclear power. The costs of implementation for these more conservative actions (whose benefits could not be, or at least were not, quantitatively estimated) were essentially linearly proportional to the required degree of plant derating. Thus the "costs" of a moratorium were a factor of 10 greater than the highest costs of imposition of the AC. A more detailed discussion of the ECCS-EIS alternatives is presented in section 3.4.

Whatever steps, or combinations thereof, are decided upon, it would seem desirable to give serious consideration to taking action that would essentially eliminate the ECCS problem as a public issue. Such a step could be as effective as those taken by the AEC in essentially eliminating the issue of radiation emissions from nuclear power plants under normal operating conditions. Faced with the alternative of continuing intervention on this subject, and given the evident engineering capability to design the plants to meet more rigorous standards, the AEC took the positive step of recommending the imposition of "as low as practicable" standards upon the industry. Utilization of these standards results in radiation emissions from power plants under normal operating conditions being at least a factor of 10 lower than background radiation. As a result of the proposed radiation standards, the issue has practically disappeared as a cause for intervention in power plant licensing.

A similar step for the ECCS reliability issue would be very desirable, if it is possible. Perhaps the most obvious step to be taken in this direction would be ECCS redesign. As previously observed, increased reflooding rates of at least 6 in/sec would result in substantially improved LOCA response for both PWRs and BWRs. At reflooding rates of this magnitude, reactor damage would be minimized and the potential hazards of substantial radiation release to the public would appear to be essentially eliminated.

To achieve flooding rates of 6 in/sec in a PWR, or equivalently heat transfer coefficients of $40 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$ or greater, would probably require redesign of the ECC fluid injection mode. Steam binding and other physical problems restrict current PWR designs to their current low predicted flooding rates. Though redesign may be expensive, it does not appear to be impossible and the resulting safety margins (with associated reduction in public risk) would appear to make the task cost effective. Even with the proposed design modifications, it is probable that a severe "design basis" LOCA would result in considerable damage to PWR reactor fuel rods,

if all the more conservative values of the critical parameters discussed above were experienced at once (though the probability of all worst case events occurring during the same LOCA is remote - see figure A10.6 of appendix 10). Even if the peak temperatures of the excursion were limited to 2200°F, blowdown temperatures of 2000°F would induce fuel rod swelling and rupture "in abundance" (60, p. 1105). Release of the gaseous and volatile fission products, normally contained within the gas gaps and rod plenums of the ruptured rods, to the reactor containment vessel would be expected. However, assuming adequate reflood rates, the LOCA scenario described does not lead to massive core melting, as envisioned in a "China syndrome" scenario. With adequate reflooding rates assured, it is not likely that damage to the containment structure would take place. Consequently, it appears that resulting radiation hazards to the general public could be kept within currently permissible standards.

With respect to BWRs, as previously discussed, there do not appear to be the same physical limitations to increasing flood rates to acceptable standards that trouble the PWRs (60, p. 1092). Simplistically speaking, increasing reflood pumping capacity appears to be a satisfactory method of resolving the problem. Review of a high reflooding rate LOCA scenario, even with the additional conservatisms previously discussed, suggests that the BWRs would probably experience less damage than would be predicted for the PWRs. In fact, a reasonable probability exists that no fuel rod ruptures would occur during the LOCA, assuming the availability of the increased reflood rates (4, p. 20-19). If this were the case, reactor damage would be minimal; and the radiation hazard to the public would not be expected to exceed normal operational limits.

Consequently, performing ECCS redesign to achieve higher reflooding rates appears to have the same potential for eliminating the ECCS reliability problem as an issue as adopting the "as low as practicable" radiation emission standards did for the issue of normal operating radiation hazards. Taking action which would eliminate this restriction to development of nuclear power seems very desirable.

If this action is found to be unacceptable by the AEC or the vendors, it may be possible to quell the argument by imposing still more conservative design criteria (as compared to the AC), and to conduct accelerated research that would provide a quantitative basis for assessment of the margin of safety.

4.3.1 Increased criteria conservatism

As discussed in the body of the text and reviewed in detail in the relevant appendices, the following changes to the AC might be considered to increase its conservatism:

- (1) Decay heat uncertainty limits increased to ANS Standard 5.1 plus 30-35 percent.
- (2) Permissible local clad oxidation limits for embrittlement lowered. An equivalent (Baker-Just) total oxidation of 12-14 percent of the total cladding thickness should be considered as a limit to increase confidence in clad ductility following quench.
- (3) The Critical Break Flow Model(s) should be specified. More definitive specifications of acceptable low quality fluid break flow models should be provided to assure conservative treatment of potential metastable fluid flows in excess of those predicted by the Moody model.
- (4) Minimum reflooding rates of 6 in/sec could be specified. Reactor vendors could be required to demonstrate that initial nominal reflood heat transfer coefficients of no less than $40 \text{ B/hr-ft}^2\text{-}^\circ\text{F}$ (or alternatively reflooding rates of no less than 6 in/sec) are attainable with their ECCS designs in the event of a double-ended pipe break DBA.

4.3.2 Accelerated research and development programs*

- (1) Programs for large scale system testing (e.g., LOFT) should be expanded and accelerated, and planning for near full-scale testing initiated. Current programs are too limited in scope and operating on too relaxed a schedule for acceptability under current LWR licensing demands. Large scale programs should be tightly coupled with a complete analytical investigation of phenomena being studied, to assure an adequate basis for transfer of test results to revised design criteria.
- (2) Fission product decay heat investigations should be conducted, including well correlated experimental and analytical studies of fuel rods with high integrated flux-time irradiation histories.
- (3) Large scale critical break flow investigations should be made of pipe flow under conditions simulating typical LWR-DBA characteristics including adequate linear dimensions and time scaling.
- (4) Additional FLECHT tests should be conducted. Tests with zircaloy clad rods at power levels associated with maximum peaking factors (in accordance with AC prescriptions) would be especially valuable.
- (5) Determination of a statistically adequate probability distribution of reactor thermal response as a function of critical LOCA-ECCS parameters should be undertaken.
- (6) Independent development of BWR-LOCA numerical analysis methods should be accelerated. A thorough cross-checking of existing BWR codes as systems, and in terms of appropriate subroutines, should be initiated as soon as possible.

* An outline of current, world-wide LOCA-ECCS R & D programs (reproduced from 10) is given in appendix 4.

4.3.3 Design concepts for improved LWR stability

During the IAC hearings, both Westinghouse and General Electric announced plans for revisions in their reactor core designs which would increase stability in the event of a LOCA. GE proposed a revised fuel bundle design as part of a larger BWR-6 system design revision. The new bundle incorporates more rods, each operating at lower linear power ratings, (64 rods vs 49, each operating at 13.4 Kw/ft vs. 18.5 Kw/ft for BWR-5) in a fuel bundle of the same basic size as previous designs.

In a similar move, Westinghouse proposed changing to a 17 x 17 rod array, designed to have the same overall dimensional envelope as their previous standard 15 x 15 array. The new fuel array "being offered for operation in 1976 or later," according to Westinghouse, is said to contain thinner fuel rods with thicker cladding. It is estimated that peak linear power density may be cut by some 20 percent by the new design.

The desirable result of such design changes are lower normal operating power (or heat) output per rod with a consequent reduction in individual rod decay heat release at reactor shutdown. Thicker cladding for PWRs, bringing them more in line with current BWR design practice, also helps to reduce the probability of embrittlement for a given oxidation exposure cycle. Thus, greater stability is achieved by the design in the event of a reactor LOCA.

To insure greater stability in all operating reactors, it is recommended that all LWR designs be investigated (including retrofits) for incorporation of such changes. Recommended, in the spirit of the GE and Westinghouse changes, are:

- (1) Reduced linear fuel rod power ratings,
- (2) Thicker rod cladding -- especially for PWRs, and additionally:
- (3) Pre-operational oxidation of fuel rod cladding,

- (4) Revised ECC fluid insertion methods to assure increased reflood rates of 6 in/sec, or more,
- (5) Design changes to reduce the effect of steam binding in PWR primary loops on ECCS performance.

4.3.4 Increased public involvement in nuclear power risk-benefit evaluations

It has been fairly observed, that "nuclear power technology is now at a point of crisis" (39). In fact, as Green has stated further:

Given the present national obsession with environmental values, the rise of (the) public-interest lawyer, public skepticism of authority, and the current judicial attitudes, nuclear power is locked in a death struggle which it cannot win, except in a Pyrrhic sense, under the present ground rules (39, p. 77).

Green's skepticism seems well founded, under the circumstances. The crisis appears to have developed on the basis of the public's perception of a breakdown in the AEC's "full, free, and frank discussion" of the hazards associated with nuclear power.

It is not the purpose of this paper to present, or defend, the causes of this perceived breakdown in the credibility of nuclear power information dissemination. However, in the development of nuclear power, as in several other areas of environmental sensitivity, it appears to be highly beneficial for all parties concerned to increase public involvement and enhance participation in decision making processes as much as possible. The concept of cooperative public/industry "open planning" of important utility decisions has been discussed at length in several EQL publications (e.g., Lees, et al., People, Power, and Pollution) (40). Demonstrated success has been shown in achieving goals of public benefit through operation of concepts involving substantial public participation in decision making such as "open planning."

Again in the words of Green:

The starting point must be a policy of "full, free, frank discussion in public" of the benefits and risks of nuclear power. The present policy of avoiding explicit discussion of risks so as to avoid "unduly alarming" the public should be abandoned. The public should be told, as a matter of course and in language that can be readily understood, what the risks are, what has been done to minimize them and what risks nevertheless remain. The public should also be told in accurate and realistic form what the benefits are.

I would like to see the nuclear safety community develop a forensic spirit. It is to everyone's advantage and in the public interest that opposition to nuclear power be channeled along constructive and responsible lines. I would hope that people working in nuclear safety would recognize a public responsibility to work with intervenors and other opponents of nuclear power--not to try to educate them as to the error of their ways, but rather to understand and accept their concerns as valid, and to help them articulate these concerns effectively, accurately, and responsibly. This, I believe, would contribute more than anything else to strengthen and promote the vitality of nuclear power and enhance nuclear safety (39, pp. 77, 78).

It is of great importance that the opportunity for meaningful public involvement in nuclear power risk-benefit decisions be increased. To achieve such involvement would be mutually beneficial for all concerned in the development of nuclear power. Open planning may not represent an instant panacea for utility company problems. In fact, it may temporarily appear to enhance problems. However, it is our belief that the American public, confronted with the decisions to be made and in possession, with understanding, of all the critical facts, will reach the right conclusions.

Appendix 1 GENERAL DESCRIPTION OF LIGHT WATER REACTOR AND EMERGENCY
CORE COOLING SYSTEM OPERATION AND DESIGN

There are two basic types of light water reactors in common use in the United States today: pressurized water reactors (PWR) and boiling water reactors (BWR). Figures A1.1 and A1.2 show, in schematic form, the elemental components of BWR and PWR power generating systems. As indicated in the figures, the principal difference between the two reactor systems is related to the isolation of the radioactively contaminated working fluid of the reactor from the turbine-generator steam supply in the PWR. As shown for PWRs in figure A1.2, steam for the turbines is produced in a "steam generator" secondary heat exchange loop isolated from the reactor fluid. High pressure and temperature water circulates through the reactor core and steam generator primary loop, while relatively lower temperature and pressure steam, developed for the turbines, circulates in the isolated secondary loop of the steam generator.

Typical operating characteristics for PWRs and BWRs are shown in table A1.1. As indicated, in order for PWRs to operate with efficiencies approximately equivalent to those of BWRs, it is necessary to operate with reactor pressures of approximately 2000 psi, nearly twice the typical 1000 psi BWR operating pressures. At these pressures, the water in the reactor portion of the PWR loop remains a liquid throughout the entire cycle. In the PWR, steam is generated only in the lower pressure (secondary) side of the steam generator loop to drive the turbine generators.

In a BWR, as indicated by its name, water passing through the core is boiled within the reactor core itself. Steam produced within the reactor is piped directly to the turbine without the added complexity

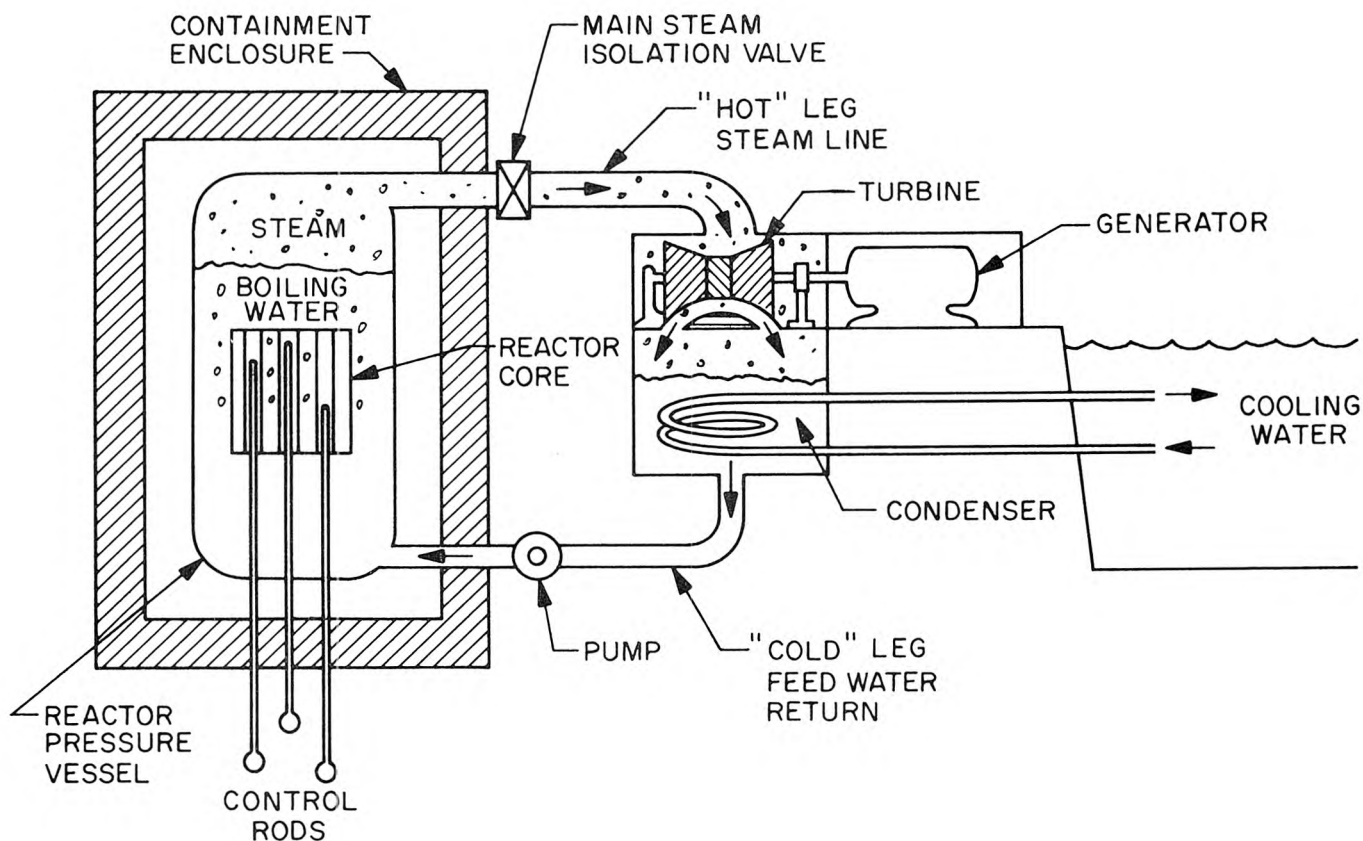


Figure A1.1 Schematic Idealization of Boiling Water Reactor Power System Components

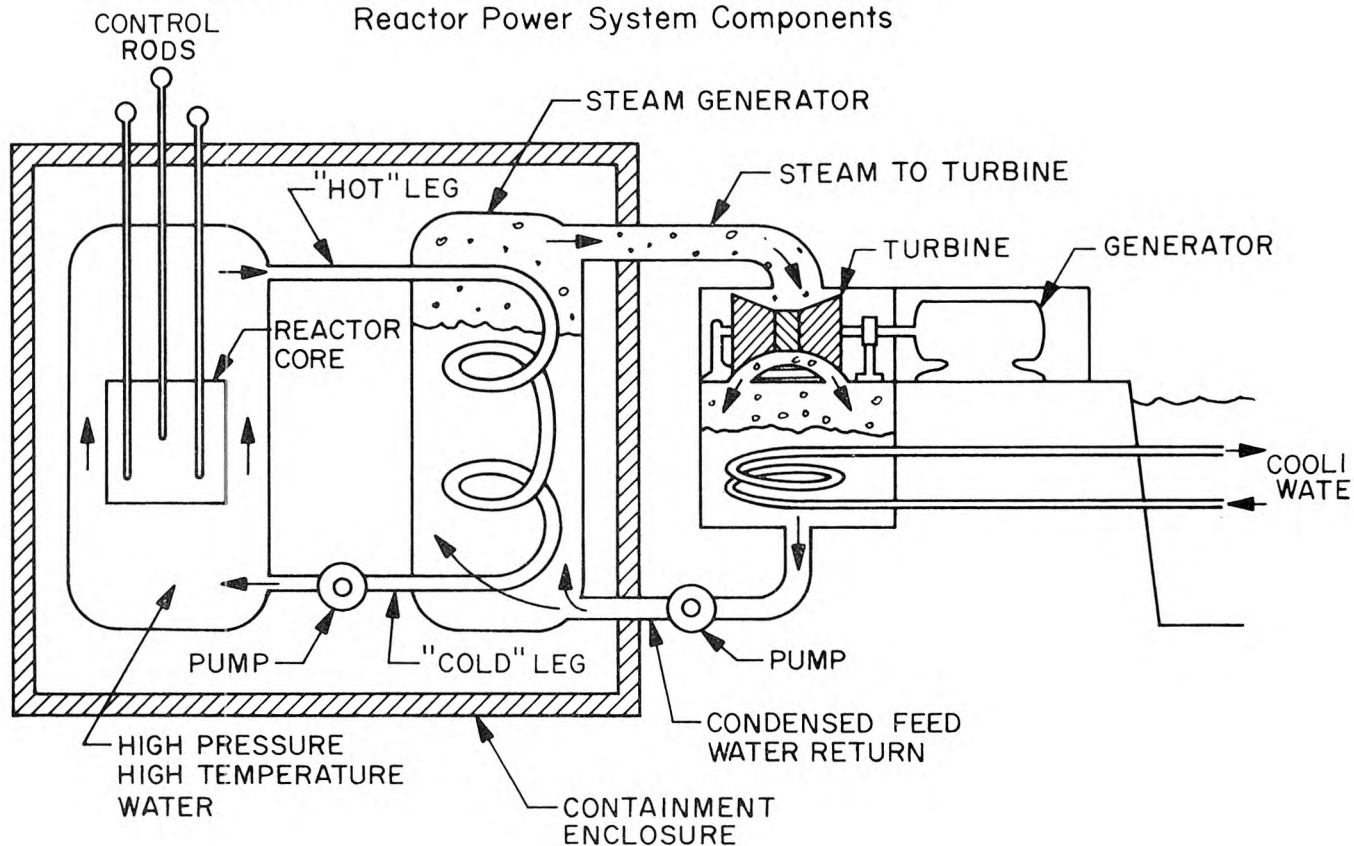


Figure A1.2 Schematic Idealization of Pressurized Water Reactor Power System Components

TABLE A1.1

Typical Operational Parameters for 1000 MW_e Light Water Reactors

<u>PARAMETERS</u>	<u>PWR</u>	<u>BWR</u>
<u>REACTOR</u>		
System Pressure (psi)	2200	1000
Inlet Temperature (°F)	550	420
Outlet Temperature	600	550
<u>TURBINE</u>		
Inlet Pressure (psi)	800	1000
Inlet Temperature (°F)	500	550
<u>THERMAL POWER RATING</u> (MW _t)	3400	3300
Electrical Power Output (MW _e)	1100	1100
<u>HEAT TRANSFER AT RATED POWER</u>		
Active Surface Area (ft ²)	53,000	66,000
Avg. Heat Flux (B/hr-ft ²)	210,000	160,000
Max Heat Flux (B/hr-ft ²)	560,000	420,000
Avg. Linear Heat Rate (KW/ft)	7	7
Max. Linear Heat Rate (KW/ft)	19	18
<u>INITIAL CORE LOADING</u>		
Initial Charge (Metric tons of uranium)	87	149
Assemblies/Core	193	764
Rods/Assembly	204	49
<u>ANNUAL DISCHARGE</u>	1/3 Core	1/4 Core
Fuel (Metric Tons of Uranium)	29	37
Assemblies	64	190
Fuel Rods	13,000	9,300
<u>FUEL RODS</u>		
Number	39,000	37,000
Outside Diam (in)	0.43	0.56
Cladding Thickness (in)	0.025	0.03
Fuel Pellet Diam (in)	0.37	0.49
Diametral Gap (in)	0.01	0.01
Active Length (in)	144	144

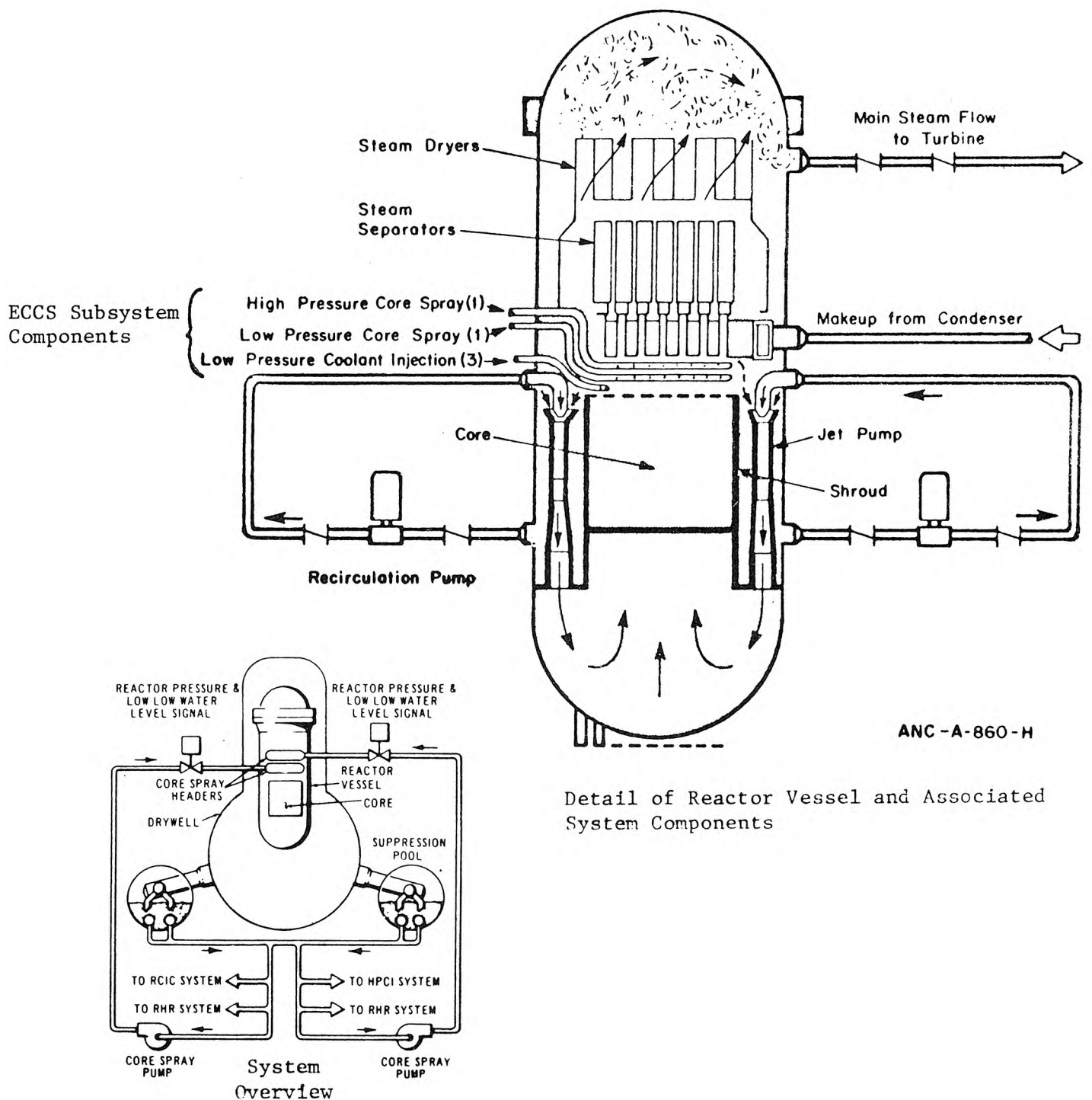
of the intermediate heat transfer loop of the PWR steam generator. In the event of a break in the BWR hot leg steam line outside the containment enclosure for the reactor, radioactively contaminated steam would be released to the biosphere. In the PWR, on the other hand, radioactively contaminated releases from a break in the reactor hot leg would be retained within the containment enclosure. In order to minimize radiation hazards to the public from external hot leg breaks, a critical BWR design feature is the main stream isolation valve which is provided to limit or prevent the escape of steam and radioactive trace elements from the reactor in the event of an accident.

More detailed (albeit still schematic) views of the nuclear steam supply systems for BWRs and PWRs are shown in figures A1.3 and A1.4. These figures show the elements of the emergency core cooling systems as well as a more accurate depiction of the working elements of the reactor and fluid flow portions of the cycle.

A1.1 BWR Steam Supply and ECCS Systems

In the BWR (figure A1.3) circulation of the water in the reactor vessel is maintained by 20 jet pumps located around the circumference of the reactor core, as shown by the two typical pumps in the cross-sectional view. Water is boiled as it flows through the core and the wet steam is separated from the entrained water droplets by steam separators. Liquid from the steam separators and the baffled steam dryers is returned to the remainder of the water in the reactor, where, combined with make-up water and condensate feed water returning from the turbines, it is recirculated through the reactor core.

A break in the recirculation loop to the jet pumps has been calculated to be the accident placing the most serious demands on the ECCS for a BWR reactor. Consequently a break in these lines has been designated the DBA for the BWR. The resulting system depressurization (blowdown) and subsequent "dryout" prior to delivery of the emergency coolant is assumed to eliminate all of the water from the reactor



SCHEMATIC DIAGRAM OF BWR STEAM SUPPLY SYSTEM SHOWING EMERGENCY CORE COOLING COOLING SYSTEM (ECCS) ELEMENTS

Figure A1.3

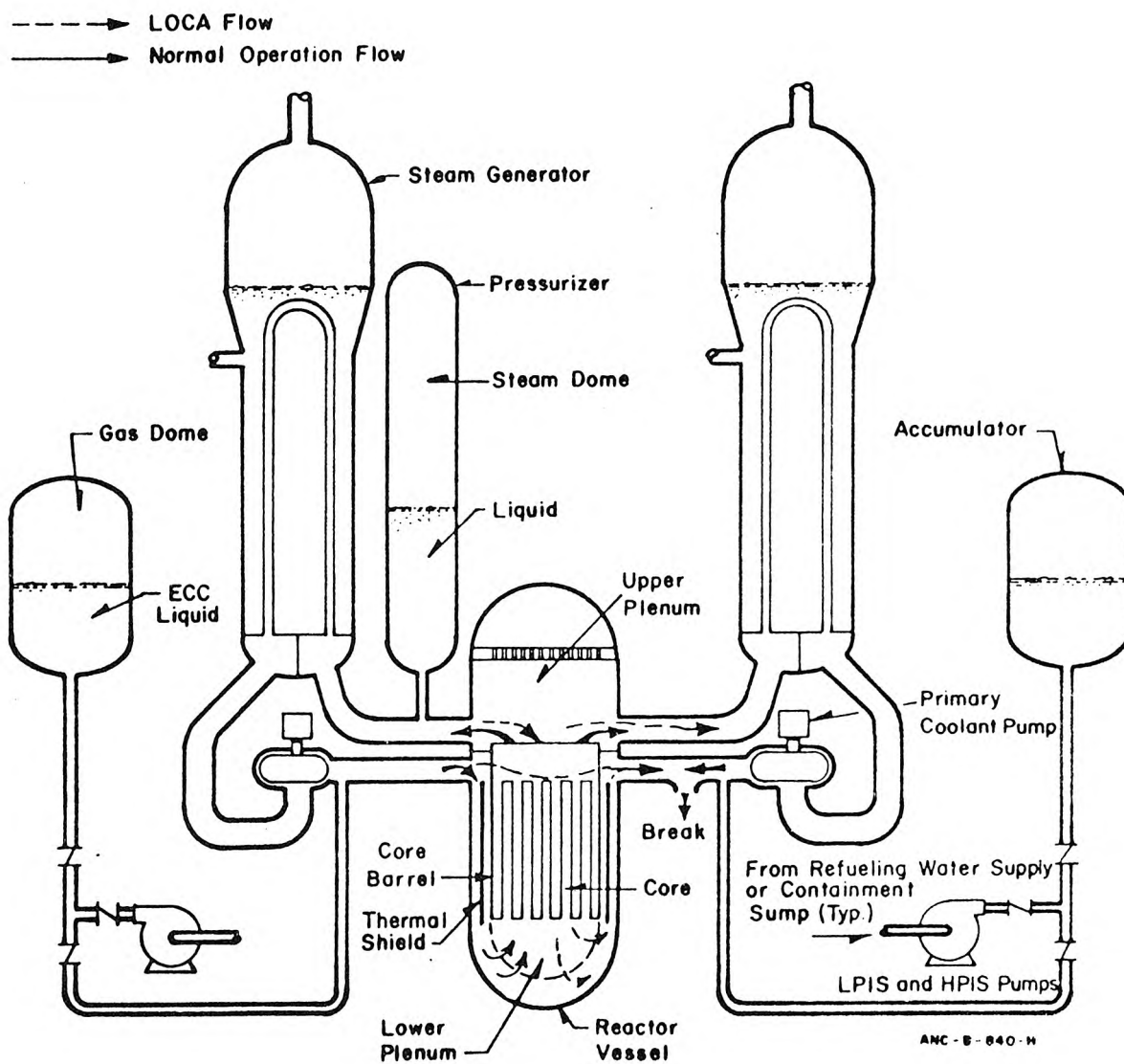
pressure vessel. Following blowdown, the emergency core coolant is delivered to the core through the circumferential ducts of the high and low pressure core spray spargers and the low pressure injection system, as shown. These three coolant injection sources are the principal means of supplying emergency coolant for the BWR-ECCS.

As water from the ECCS accumulates in the reactor core, levels as high as the tops of the jet pumps can be maintained for long term reactor cooling. Fluid lost from the recirculation loop break is collected in a fluid reservoir "pressure suppression chamber" within the containment vessel for the reactor. The pressure suppression chamber reservoir acts as a sink for condensation of steam from the break as well as a source for long term recirculation of coolant to maintain the core temperatures in a safe steady state condition.

A1.2 PWR Steam Supply and ECCS Systems

In the PWR, a break in the "cold leg" main inlet line from one of the steam generator loops has been calculated to produce the most severe fluid loss conditions for the reactor and is consequently used as the DBA. As indicated in figure A1.4, all primary system components and inlet and outlet pipes are located above the reactor core. This design increases the potential for emergency coolant to refill the reactor vessel above the core. Furthermore, penetrations in the vessel below the core are avoided in the design of PWRs in order to limit the possibility of breaks occurring in the vessel which could result in serious coolant losses.

Immediately following the LOCA the principal sources of coolant for the PWR-ECCS are the gas pressurized accumulators as shown in figure A1.4. An accumulator tank is provided for each of the steam generator loops for the reactor (from two to four individual loops depending upon manufacturer's designs). The accumulator tanks are typically designed to operate automatically, through check valves, when



SCHEMATIC DIAGRAM OF PWR STEAM SUPPLY SYSTEM SHOWING EMERGENCY CORE COOLING SYSTEM (ECCS) ELEMENTS

Figure A1.4

the reactor pressure falls below 600 psi. Without losses, the tanks are designed to refill the core with borated water (to "poison" further nuclear reactions) to a level of one-half the length of the fuel rods, within one-half minute after a large pipe break.

The residual heat removal system of the ECCS for a PWR operates after the accumulator tanks have begun their delivery when the system is essentially depressurized. As indicated in figure A1.4, this part of the system supplies fluid from the refueling water supply and/or the containment sump through the low pressure injection system (LPIS) and high pressure injection system (HPIS) pumps. Pressure suppression chambers are not a characteristic of PWR design as they are for the BWR. PWR fluid losses during blowdown following DBA are, however, collected in the containment vessel and recirculated from the sump heat exchanger system for long term, steady state cooling of the reactor following a LOCA. Thus steady state cooling methods are quite similar for both BWRs and PWRs.

The portion of the PWR primary steam system labelled "Pressurizer" in figure A1.4 is not an integral part of the ECCS. In a PWR, a single "Pressurizer" is provided for normal operating conditions to act as a fluid oscillation damping-energy absorbing reservoir, in order to compensate for electrical load following demands on the steam generators which may require changes in the steam supply which would be too rapid for normal reactor load following capability. The fluid in the pressurizer is considered a part of the operating system, all of which is assumed to be lost during blowdown. Consequently no beneficial delivery from the pressurizer is assumed for the ECCS although, practically speaking, some benefit might be expected to be gained in an actual LOCA.

A1.3 Radioactivity*

The total amount of radioactivity in an operating nuclear power plant depends on the reactor's power level and time in operation. When a

* Abstracted from (1) pp. 4-6 to 4-8.

light water nuclear plant (LWR) in the 1000 MW_e size range is first placed into operation, for example, it is loaded with unirradiated, but naturally-radioactive uranium fuel (enriched to about 3 percent U-235) having an aggregate activity of about 150 curies in a typical PWR loading and about 300 curies in a BWR loading. (Table A1-1 lists some typical PWR and BWR fuel loading and discharge data for reference.) With nuclear operation at power, the quantity of radioactivity increases to the order of 1.7×10^{10} curies between refueling operations, which occur about once a year (see table A1.0). Since only a fraction of the core is replaced during refueling, a large inventory of radioactive material is retained in the core after initial power operation. The quantity would be greater or smaller for the same kind of plant with larger or smaller power level, respectively.

When the reactor is shut down, the generation of radioactivity ceases and the quantity of radioactivity in the spent fuel decreases, initially at a very rapid rate due mostly to the decay of short-lived fission products to longer-lived or non-radioactive nuclides. At the same time, substantial quantities of heat continue to be generated in the spent fuel due to the interaction of the intense radiation of the decaying radionuclides with atoms and molecules in the spent fuel and surrounding media. The amount of heat generated decreases with time as radioactive decay progresses; this heat is called "decay heat" (see appendix 5).

Table A1.2 provides some calculated values for the quantities of radioactivity and heat associated with the entire core of a 1100 MW_e PWR after a sustained period of operation. The values indicate how these quantities would decrease with time after reactor shutdown. To a good approximation, these values would also apply to a comparable BWR core under the same circumstances. The values in table A1.2 are based on the assumption that the whole core loading of fuel is allowed to decay.

Table A1.2
Calculated Radioactivity of 1100 MW_e PWR* at Shutdown and
as a Function of Decay Time

Decay Time (days)	Radioactive (megacuries)					Total	Total Thermal Power (kW)
	Iodine and Bromine Isotopes	Noble Gases	All Fission Products	Actinides	Activation Products		
0	1,435	1,240	13,800	3,450	10.6	17,250	225,000
1	265	221	2,890	1,330	9.19	4,230	17,400
5	101	105	1,870	432	8.42	2,310	9,720
15	28.7	29.0	1,280	39.7	7.50	1,330	5,600
30	6.74	4.77	947	9.35	6.40	963	4,060
60	0.494	0.784	656	6.32	4.76	666	2,350
120	0.00282	0.659	401	5.90	2.76	410	1,740
210	0.00000309	0.648	244	5.56	1.36	250	1,100
365	0.00000218	0.630	146	5.17	0.614	152	659
1,097	0.00000218	0.553	47.3	4.45	0.324	52.0	204
3,653	0.00000218	0.353	17.9	3.27	0.132	21.3	67

A1-10

* Reactor is assumed to be shut down just before refueling after a sustained (293-day) period at a specific power of 37.5 MW/metric ton. The time average specific power over the previous 1100 days is 30 MW/metric ton. The reactor is fueled with 3.3% enriched uranium totaling 82 metric tons of enriched uranium fuel.

Actually, as shown in table A1.1, generally only one-third of the fuel in a PWR (one-fourth in a BWR) is removed and replaced with fresh fuel each year, so that most of the fuel remains in the reactor from three to four years.

The portion of irradiated fuel discharged annually from LWRs is stored in water pools in the reactor plants. These storage pools provide cooling for the decay heat and shielding for plant operators against the intense radiation of the spent fuel. After about 150 days storage, the radioactive rate of decay in the spent fuel has slowed considerably. By this time, many of the shorter-lived radionuclides have decayed to non-radioactive species and the continuing decay is paced by the longer-lived radionuclides in the fuel. Longer storage of 30 to 60 additional days does not result in substantial further reduction in total radioactivity. Consequently, the spent fuel is loaded into heavily-shielded shipping casks for transfer to a fuel reprocessing plant after about a 150 day cooling period.

Appendix 2 GENERALIZED DESCRIPTION OF LOSS-OF-COOLANT ACCIDENT FOR PWR's AND BWR's

The events and processes of an LOCA are developed to illustrate the system behavior and phenomena which must be accounted for by the calculational methods in order to prescribe the design and performance requirements for ECC systems.

PWR-LOCA BEHAVIOR

Figure B-1 depicts the generalized LOCA behavior for a postulated large break in one loop of a multiloop PWR primary coolant circuit. This characterization of the accident is derived from many calculations carried out for different pipe break locations for current PWR designs. The numbers on the figure are indexed to the following description of the course of the loss-of-coolant process.

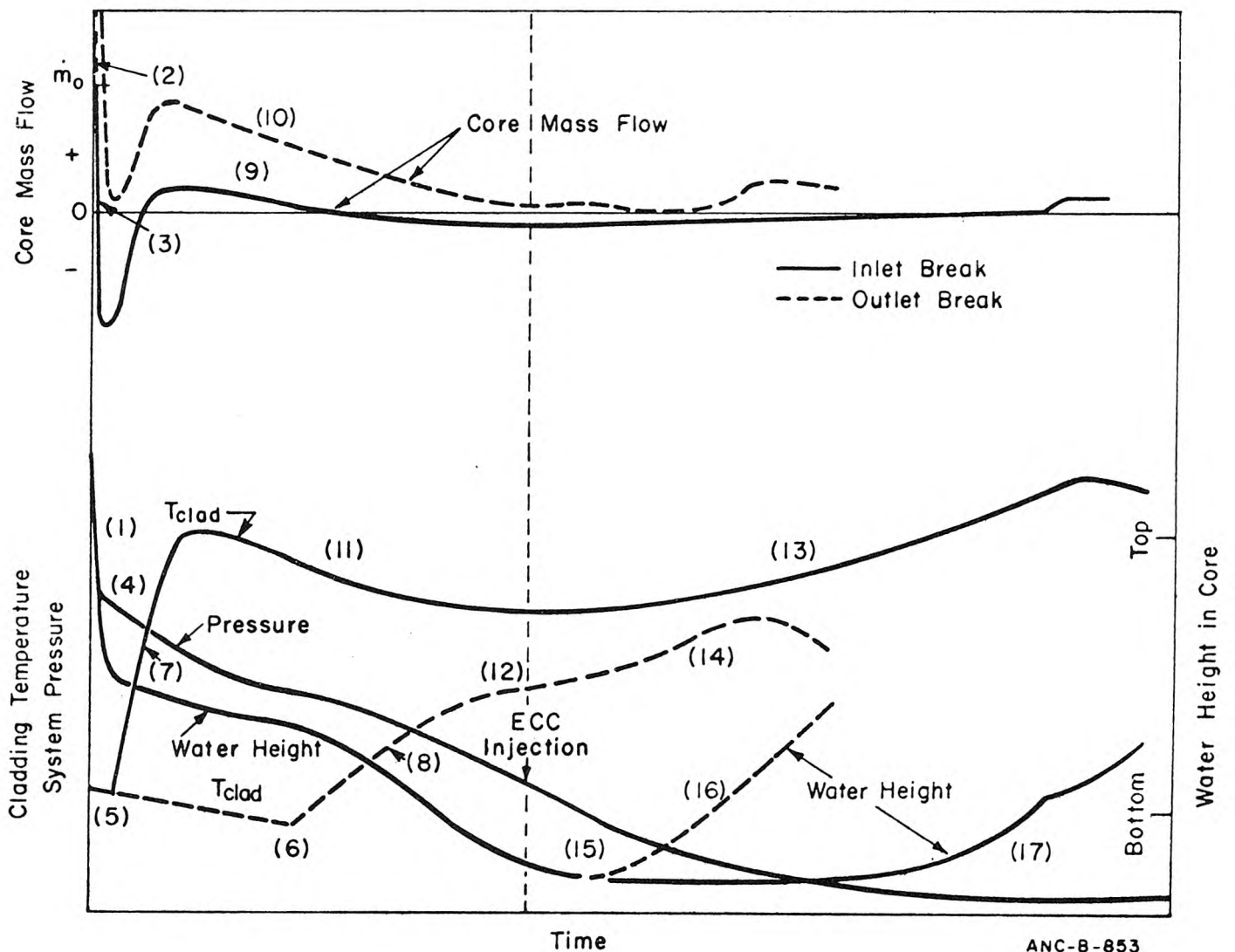


Figure A2.1 Generalized Loss-of-Coolant Behavior for Large Pipe Breaks in a PWR.

Immediately following the pipe break, as the primary coolant is expelled from the rupture, the system experiences a rapid subcooled depressurization (1) causing the flow within the reactor core to accelerate for an outlet break (2) or decelerate for an inlet break (3). As the system depressurization continues (4) the local fluid saturation pressure is reached and fluid flashing, with an attendant fluid density decrease, occurs in the core as steam bubble growth is initiated. Within the core region the decreasing fluid density (moderator loss) causes the core power generation to decline within a few hundred milliseconds to the fission product decay heat power level (approximately 6% of the operating power).

For the inlet break conditions the reduced core flow commensurate with coolant voiding in the core can cause a large abrupt decrease in heat transfer from the fuel to the coolant and initiate the critical heat flux (CHF), or departure from nucleate boiling (DNB) (5). For the outlet break condition the core flow increase (2) tends to offset the density decrease and high heat transfer is preserved for an extended period until the local fluid conditions within the core are degraded sufficiently that CHF (6) ultimately is reached. For either break, the abrupt decrease in heat transfer (5) (6) allows the large amount of stored energy within the fuel to redistribute with a resultant rapid increase in fuel cladding temperatures (7) (8).

For the inlet break condition, at several seconds into the depressurization process the core mass flow rate (9) is significantly reduced because of nearly balanced fluid resistance paths to the break. For the outlet break condition, the fluid resistance to the break from the core region is markedly lower and results in a continued significant upward core flow (10). These differences in the core flow histories (9) (10) respectively influence the cladding temperature histories for the inlet and outlet break conditions.

As the stored thermal energy within the fuel becomes redistributed the cladding temperature rise may terminate or the temperature may decline slightly (11) (12) as the competitive effects of continued fission product decay heating and some limited amount of heat transfer exist for a few seconds. As the coolant conditions within the core continue to deteriorate the cladding temperature rises (13) (14) commensurate with adiabatic conditions dictated by the local fission product decay heat rate.

When the cladding temperature exceeds $\sim 1200^{\circ}\text{F}$ for either break condition, structural distortion, such as ballooning of the cladding, may develop. Ballooning is postulated to result from a combination of the decreased strength of the cladding (as the temperature increases) and the increasing differential pressure between the internal fuel rod pressure and the decreasing external system pressure.

As the coolant is expelled into the containment structure surrounding the reactor, the primary system continues to depressurize with an accompanying decrease of liquid level within the reactor vessel (15). When the system pressure decreases below the gas dome pressure within the ECC accumulators (or core flooding tanks), relatively cold auxiliary coolant is injected into the appropriate inlet piping (or upper core barrel region) in an attempt to replenish the liquid inventory in the bottom plenum of the reactor vessel.

For an outlet break condition, soon after accumulator injection begins, the liquid inventory in the bottom plenum is replenished to the bottom of the core (16). Core flooding is maintained by the low pressure coolant injection systems when the accumulator inventory is spent.

For an inlet break, some backflow from the core and continued boiloff of the liquid in the lower plenum cause steam flow up the downcomer which tends to inhibit the entry of auxiliary coolant to the lower plenum. In addition, the steam flow in the inlet pipes of the unbroken loops tends to entrain some of the injected coolant and this entrained coolant is then carried around the downcomer annulus to the break. These conditions lead to the postulated "accumulator ECC bypass" situation. As decompression continues and the system steam flow rates decrease, the influence of gravity overcomes the entrainment forces and the lower plenum begins to fill (17).

As the lower plenum fills and coolant reaches the bottom of the core, steam begins to be generated. The steam, entraining some liquid, rises in the core and cools the cladding. For the inlet break, the steam must escape from the system by passing through the steam generators and pumps in order to reach the system vent, or pipe break (Figure A-1 of Appendix A). The steam, in passing through the various system components and particularly the steam generator where additional energy is added from the secondary system, is impeded by friction. The frictional pressure drop can reach a value of several pounds per square inch causing a backpressure on the reflooding process which competes against the head of water in the downcomer attempting to drive coolant into the core. The downcomer head in most current reactor designs can develop to a maximum head of $\sim 7\frac{1}{2}$ psi if the flooding process is relatively steady-state. If oscillatory effects occur as a result of the coupling between the inertance and the force of gravity on the liquid in the downcomer and the coupling between the inertance of the liquid in the downcomer and the compliance of the compressible steam volume above the flooding front, the average driving head could be less than the steady-state driving head, thus lowering still further the time-averaged flooding rate within the core. The oscillatory flooding front could, however, provide improved heat transfer in the early part of the flooding process.

The competing effects of the limited driving head for flooding and the backpressure from the exiting steam give rise to the postulated steam binding problem. The limited flooding rate for the inlet break, resulting from steam binding, causes decreased heat transfer in the core relative to that which would exist for higher flooding rates for an outlet break. Where these competing effects are involved for the inlet break, additional subtleties, such as the effects of containment backpressure on entrainment and on heat transfer and such as the compressible-flow acceleration-pressure drops due to energy being transferred from the secondary side of the steam generator to escaping steam from the primary system, become important.

The temperature that the fuel cladding can attain without loss of structural integrity is determined, for zirconium-clad fuels, by the amount of oxygen taken up by the cladding during metal-water reactions which become significant at temperatures above 1800°F. At 1800°F the reaction rate is low but as the temperature increases to 2000°F and above, the reaction rate increases rapidly. At 2300°F, for example, the oxygen uptake is such that reaction durations exceeding several tens of seconds cause sufficient embrittlement that upon quenching of the fuel cladding by ECC, the structural integrity of the cladding is insufficient to assure a definable heat transfer geometry within the hotter regions of the core.

The foregoing, intended to depict the general system behavior during an LOCA for a PWR, has emphasized the DBA conditions which are expected to establish the ultimate requirements for ECC system design. The magnitude of the calculated effect of break size and location on the DBA is shown in Figure B-2. The calculations for developing the figure include those by the reactor manufacturers and those performed independently by Aerojet Nuclear Company in conjunction with the design and program planning for the Loss-of-Fluid Test (LOFT). Apparent from the figure is the dominant influence of the large inlet break in determining the requirements of ECC designs for PWR's. However, the very largest break should not be concluded to be the most demanding on ECC design for all PWR's.

BWR LOCA BEHAVIOR

Figure B-3 depicts the generalized loss-of-coolant accident behavior for a postulated break in either the liquid recirculation lines or the steam outlet lines of a contemporary boiling water reactor system. The numbers on the figures are indexed to the following description of the course of the loss-of-coolant process.

Immediately following a steam line or recirculation line break of a BWR the system experiences a very limited subcooled depressurization because a significant amount of the fluid in the system during operation is at saturation conditions, with the remainder being slightly subcooled. The loss of one recirculating loop causes the core mass flow to drop rapidly to about one-half the initial value (1) as the other systems continue to provide coolant supply to the lower plenum, since a large volume of the vessel contains steam, at the outset, the depressurization process is relatively slow (2), and at several seconds into the transient, the steam isolation valves in the outlet line close requiring that all system coolant exit from the pipe break region. Since the contemporary version of the BWR incorporates the internal jet pump design, all pipe breaks, including recirculation and steam line breaks, in general, produce the effect of an outlet break in a PWR; that is, the depressurizing coolant flows in the normal upward direction through the core as illustrated in the figure.

At the approximate time the liquid level within the reactor drops to an elevation at which the jet pumps become uncovered, the mechanical pumps in the recirculation line are coasting down and shortly cavitate dropping the core mass flow to nearly zero. These conditions promote coolant starvation within the reactor core and initiate CHF in the hotter regions of the core (3). As the liquid level in the outer annulus around the core barrel drops to the elevation of the recirculation line outlet, the flow out the break becomes steam and the depressurization rate is increased (4). Simultaneously, the saturation pressure of fluid in the lower plenum of the reactor vessel is reached and a process called lower plenum flashing is initiated (5). During this process the fluid tends to flash violently and surges into the core region. The potential for significant cooling exists such that the cladding temperature rise may be terminated (6) and the cladding temperature may be restored to the fluid saturation temperature. As the coolant inventory in the lower plenum is spent from flashing, the system pressure continues to decline and the cladding temperature again rises in the hotter zones of the core and experiences DNB a second time (7). The cladding temperature rises rapidly until the energy redistribution within the fuel pin is complete at which time decay heat limits the rate of the temperature rise (8). Shown in the figure for completeness is the continued temperature rise from the early event of CHF (3) on through to the temperature limit (9') assuming no cooling due to lower plenum flashing. As the system pressure continues to drop, a high pressure spray system above the reactor is initiated and top spray flow is developed at about 260 psia. The spray tends to wet the fuel canister walls providing a radiation sink for heat removal from the fuel pins. The resulting steam from canister wetting also provides some convective heat removal from the cladding surface. This cooling process tends to slow the heatup rate until the lower reactor vessel plenum is filled by the accumulated spray and LPCI system coolant inventories and a core reflooding process similar to that for the PWR is initiated.

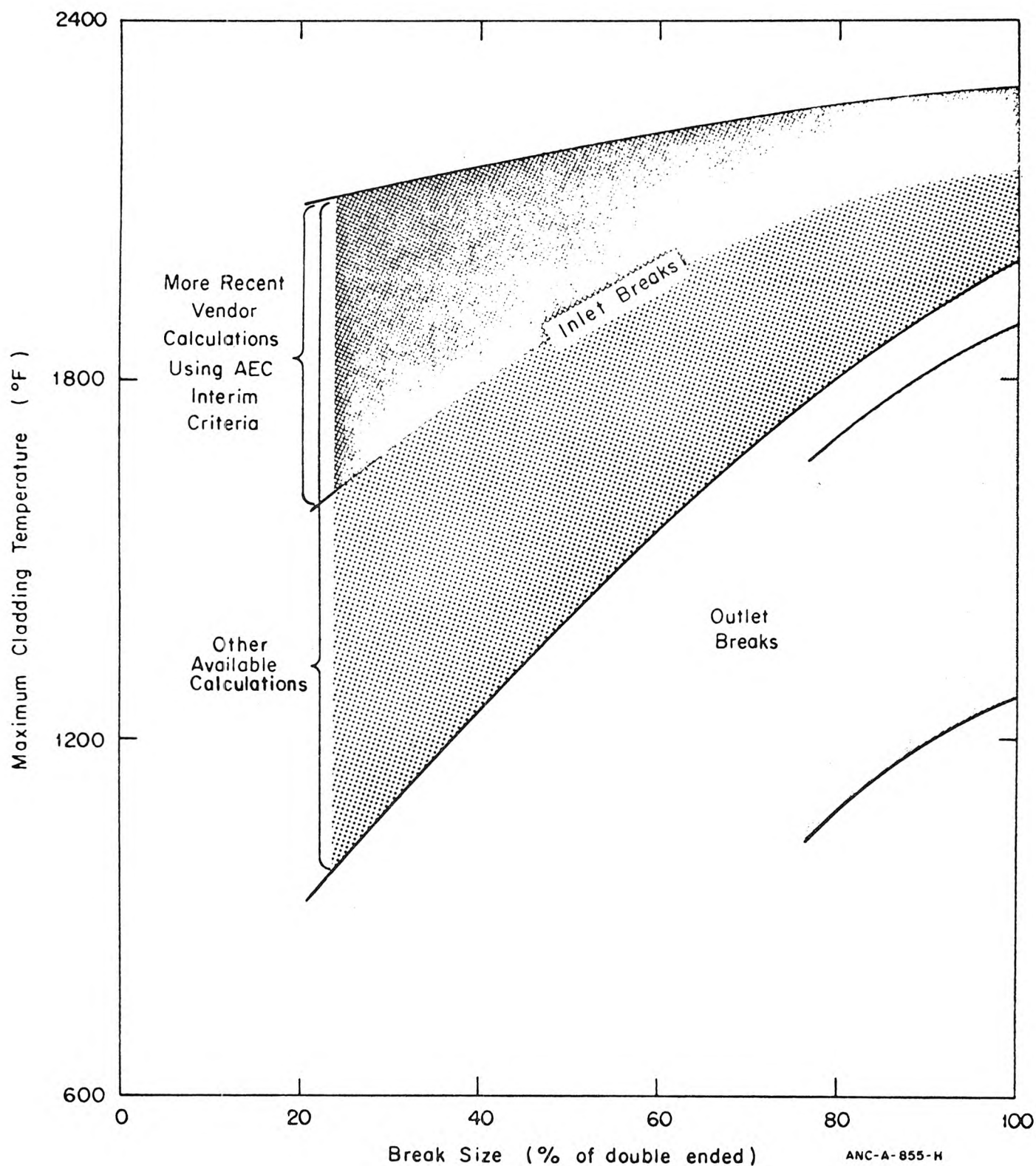


Figure A2.2 Generalized Comparison of Maximum Cladding Temperature for Various Primary System Pipe Break Conditions in a PWR.

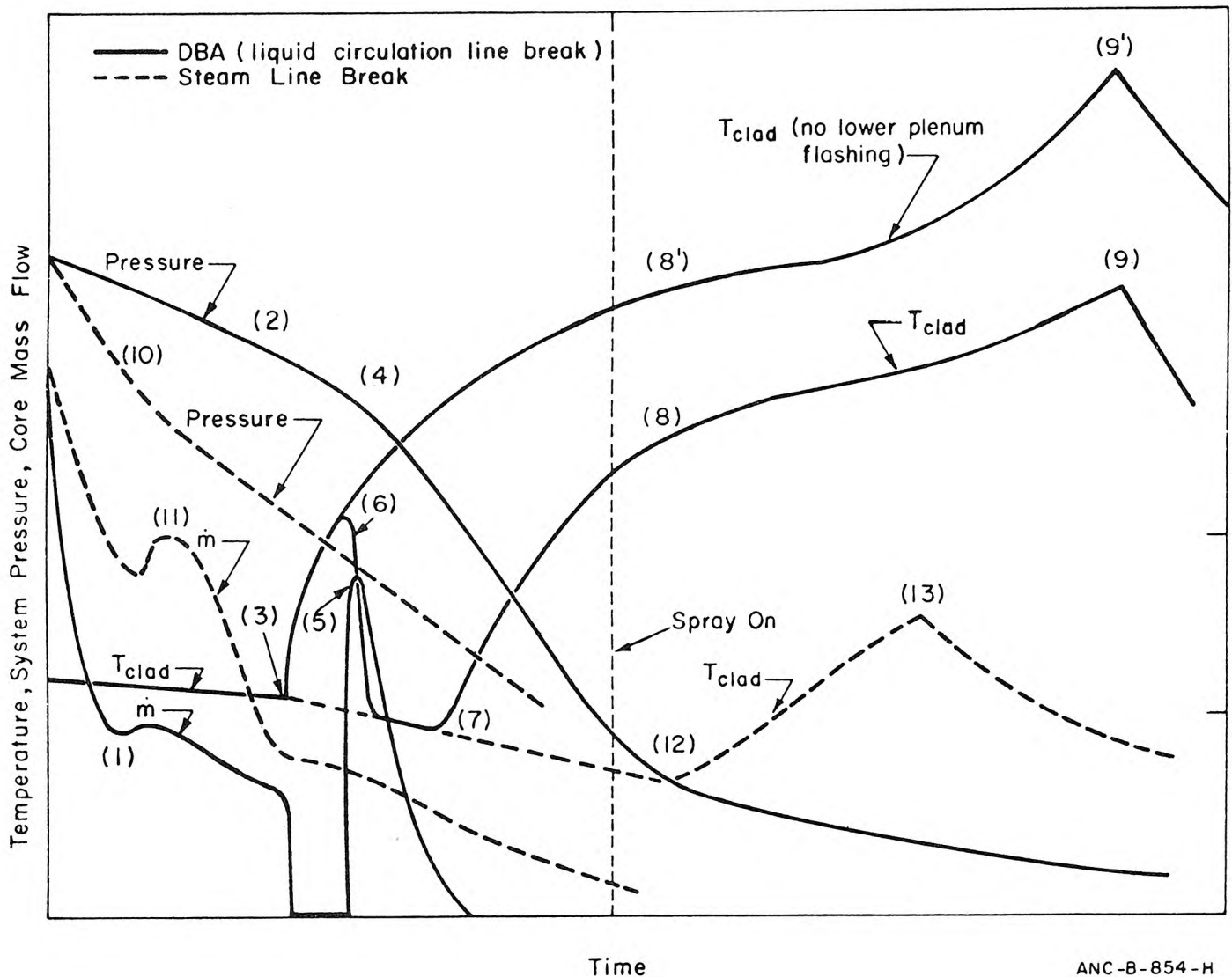


Figure A2.3 Generalized Loss-of-Coolant Behavior for Large Pipe Breaks in a BWR.

For the lower cladding temperature history (8), the cladding temperature turnaround (9) results from the initiation of flooding at the bottom of the core. For the upper cladding temperature history (8'), the effects of metal-water reaction energy are seen to cause a significantly increased rate of temperature rise prior to the event of flooding (9').

For the steam line break, the various events are depicted by dashed lines in Figure B-3. The pressure is seen to decrease considerably more rapidly (10) than for the recirculation line break. Since steam venting is taking place at a higher region of the reactor vessel the liquid fraction in the system remains high and all recirculation line systems continue to operate. Significant core flow is thus seen to continue (11); however, the flow eventually decreases as the pressure decay causes the recirculating mechanical pumps to cavitate. The flashing process continues to provide reasonable core flow and at least sufficient steam cooling to the core. The attendant cladding temperature indicates that nearly all the stored energy within the fuel is removed until, at the worst case, the coolant conditions can no longer support the heat transfer required to keep the cladding temperatures near the coolant saturation temperature (12). At this point the cladding temperature begins to rise as a result of the small amount of remaining stored energy and decay heat energy. Up to this time most of the fluid lost from the system as a result of a steam line break has been steam and some two-phase mixture; that fraction of liquid having insufficient enthalpy to flash remains in the lower plenum. The additional inventory necessary to fill the lower plenum to the bottom of the core and effect early turnover of the cladding temperature rise (13) is, therefore, considerably less than for the recirculation line break. The general behavior of the fuel cladding, effect of metal-water reaction, and embrittlement are sufficiently similar to those of a PWR that additional discussion is unwarranted.

Unlike the process for the PWR, the effect of steam binding does not appear to inhibit the rate of flooding because the steam need pass only through relatively small frictional pressure drop paths (Figure A-4 of Appendix A) on its way to the break.

Figure B-4 presents the calculated peak cladding temperature as a function of break area for steam line and recirculation line breaks for two separate single failure conditions in a contemporary BWRB-1. One case considers failure of the HPCS; the other considers failure of a diesel generator. These graphs are considered representative of a single-failure criterion approach to maximum cladding temperature and should not be considered to be restrictive in defining the capability of a system or combination of systems. As would be expected, a general trend toward higher peak cladding temperatures occurs as break areas increase. For the smallest breaks, no core heatup occurs. The exact shapes and magnitudes of the temperature curves for this type of representation depend to a large extent on such factors as the analytical techniques used in the calculations, assumptions on heat transfer correlations, and the particular single failure condition considered.

REFERENCE

B-1 Core Standby Cooling Systems for the General Electric 1969 BWR Standard Plants, NEDO-10183 (May 1970).

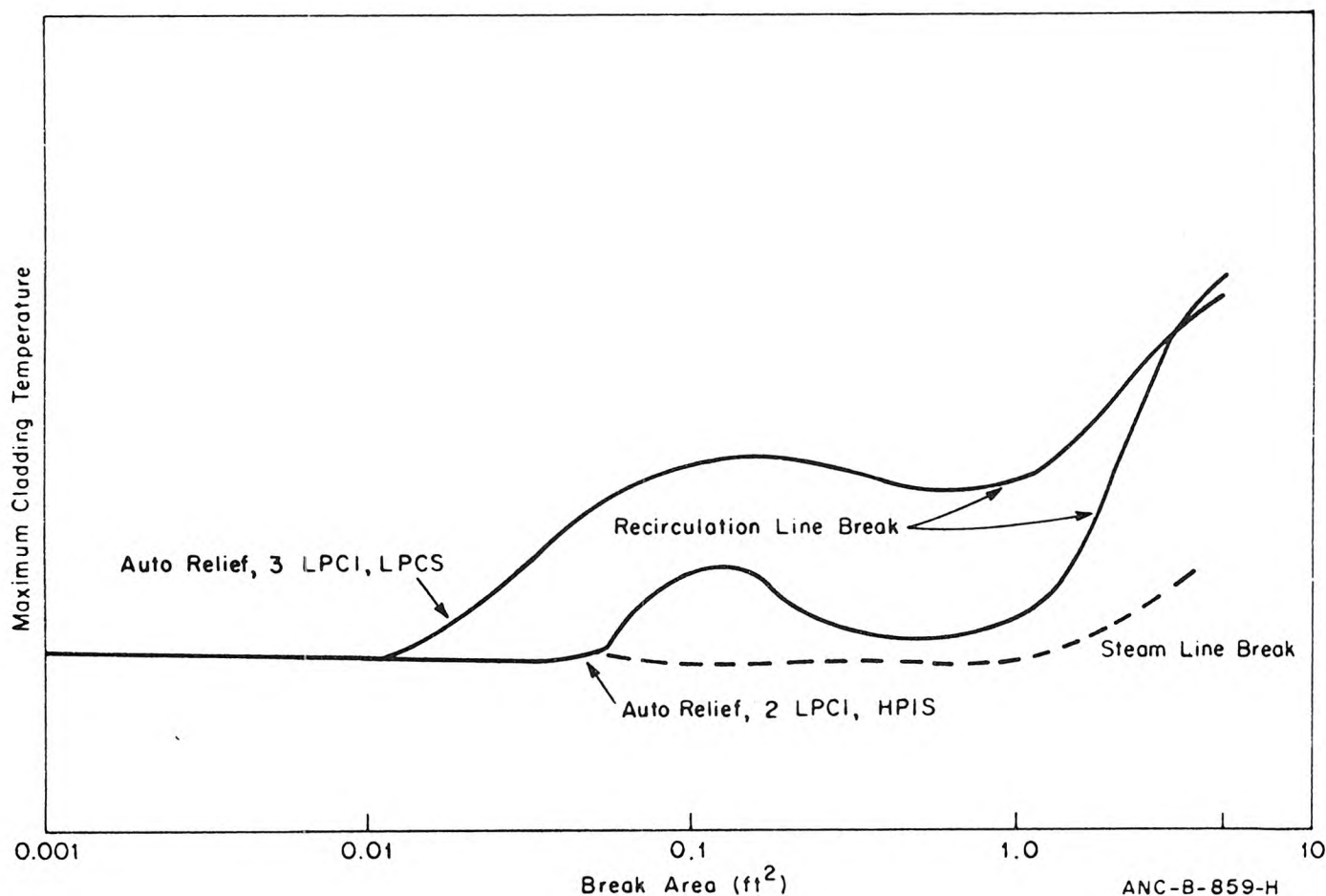


Figure A2.4 Generalized Comparison of Maximum Cladding Temperature for Various Pipe Break Conditions in a BWR.

Reprinted, by permission, from Brockett, G.F., R.W. Griebel, R.W. Shumway and J.O. Zane, "Loss of Coolant" Control of Consequences by Emergency Core Cooling", Proceedings of the International Conference on Nuclear Solutions to World Energy Problems, Joint ANS/AIF Conference, November 17, 1972, Washington, D.C. (Reference 10) and American Nuclear Society.

ATOMIC ENERGY COMMISSION

[Docket No. PRM-36-1]

CRITERIA FOR EMERGENCY CORE COOLING SYSTEMS FOR LIGHT-WATER POWER REACTORS

Interim Policy Statement

The Atomic Energy Commission has adopted the interim statement of policy set forth below providing interim acceptance criteria for emergency core cooling systems for light-water power reactors.

INTERIM ACCEPTANCE CRITERIA FOR EMERGENCY CORE COOLING SYSTEMS FOR LIGHT-WATER POWER REACTORS

I. GENERAL

The Atomic Energy Commission has recently been reevaluating the theoretical and experimental bases for predicting the performance of emergency core cooling systems, including new information obtained from industry and AEC research programs in this field. As a result of this reevaluation, the interim criteria of section IV of this policy statement have been adopted by the Commission for use in the licensing of light-water power reactors.

II. BACKGROUND

Protection against a highly unlikely loss-of-coolant accident has long been an essential part of the defense-in-depth concept used by the nuclear power industry and the AEC to assure the safety of nuclear power plants. In this concept, the primary assurance of safety is accident prevention by correctly designing, constructing, and operating the reactor. Extensive and systematic quality assurance practices are required and applied at every step to achieve this primary assurance of safety. Nevertheless, deviations from expected behavior are postulated to occur, and protective systems are installed to take corrective action as required in such events. Notwithstanding all this, the occurrence of serious accidents is postulated, in spite of the fact that they are highly unlikely, and engineered safety features are installed to mitigate the consequences of these unlikely events. The loss-of-coolant accident is such a postulated improbable accident; the emergency core cooling system is one of the engineered safety features installed to mitigate its consequences.

Emergency core cooling system design considerations were reviewed in a 1967 report to the AEC by an ad hoc Advisory Task Force on Power Reactor Emergency Core Cooling. The Task Force recommended that additional assurance could and should be obtained that substantial fuel melting can be prevented by emergency core cooling systems. Improve-

ments in primary system integrity, development of improved analytical methods for predicting core cooling performance, and performance of confirmatory experiments were recommended.

Extensive design, analysis, and research programs were initiated by the AEC and the nuclear industry in these areas, and much new information has been developed. Additionally, practices in the design, manufacture, installation, and inspection of power reactor primary systems have been markedly improved.

Later, in 1969, an AEC Internal Study Group recommended greater emphasis on quality assurance, and confirmed the use of postulated unlikely accidents (such as the loss-of-coolant accident) as design bases for reactor safety.

The ongoing industry and AEC programs have produced a large amount of information not available at the time of the earlier reviews. This new information has led to changes in the various emergency core cooling system designs for power reactors, and also in the analytical methods used in the evaluation of system performance. Development by the reactor vendors, and independently by the AEC, of new methods of analysis—computer codes—more complex and sophisticated by far than those formerly in use, gave new insight into the processes, and problems, in predicting emergency core cooling system performance.

The nuclear industry as well as the AEC has sponsored a great deal of confirmatory experimentation in this field. Blowdown experiments performed on nonnuclear simplified models of pressurized systems were used to check and correct the new codes. Some of these experiments in the small LOFT Semiscale Blowdown System at the National Reactor Testing Station in Idaho showed deviations from the predictions of the codes then in use. For example, the emergency core cooling water was ejected from the system during the blowdown. Although there are differences between the small LOFT Semiscale experiments and large power reactors, this experimental result has been taken into account where applicable in the evaluation models of Appendix A by including the conservative assumption that all of the water injected by the accumulators during blowdown is lost.

The process of code development and experimentation using models is expected to continue. The Commission plans to place the necessary additional emphasis on such work in Commission programs and expects the nuclear industry to accelerate its efforts.

In view of the large amount of new information available, the AEC has again conducted a review of the present state of emergency core cooling system technology, and has reevaluated the basis previously used for accepting system designs for current types of light-water reactors.

III. EVALUATION OF EMERGENCY CORE COOLING SYSTEM PERFORMANCE

The course of a loss-of-coolant accident, and the performance of the emergency core cooling system, are evaluated with a sequence of calculations. For calculation, the system is divided into many control volumes ("nodes"). Each volume contains the heat sources and sinks appropriate to the component being modeled. During the entire calculation, temperatures in the core are calculated as function of time. The cooling processes are primary coolant flow during blowdown and flow of emergency core cooling water as it becomes available.

Ideally, one would have available analytical methods capable of detailed realistic prediction of all phenomena known or suspected to occur during a loss-of-coolant accident, supported in every aspect by definitive experiments directly applicable to the accident. In the absence of such perfection, adequate assurance of safety can be obtained from an appropriately conservative analysis based on available experimental information. In areas of incomplete knowledge, conservative assumptions or procedures must be applied. When further experimental information or improved calculational techniques become available, the conservatism presently imposed will be reevaluated and a more realistic approach will be taken.

Detailed technical reviews have been performed by the AEC of the computer codes currently available for predicting emergency core cooling system performance. The AEC has developed sets of suitably conservative assumptions and procedures which together with the computer codes comprise three appropriately conservative evaluation models to use for evaluation. The codes used in one of these evaluation models (described in Part 1 of Appendix A) are available from the AEC. Codes used in the other two evaluation models (described in Parts 2 and 3 of Appendix A) contain proprietary material, for which summaries are or soon will be publicly available. Other evaluation models are under review by the AEC.

The three acceptable evaluation models presently included in Appendix A are different in many respects, and the sets of conservative assumptions and procedures also differ from one another. These differences arise from two principal causes: (1) Differences in approach and calculational methods of the different analyses, leading to different areas where imperfect knowledge or analysis require conservative treatments, and (2) differences in hardware among the various reactor designs, such as spray vs. flood cooling and hot leg vs. cold leg vs. direct vessel injection.

IV. INTERIM ACCEPTANCE CRITERIA FOR EMERGENCY CORE COOLING SYSTEMS

The criteria for acceptance of emergency core cooling systems have been developed in the context of the defense-in-depth concept, with the primary as-

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insurance of safety being accident prevention, achieved by correct design, construction, and operation and by adequate quality assurance. The loss-of-coolant accidents postulated in the criteria thus presuppose a highly unlikely event as a starting point.

These criteria are applicable to all light-water power reactors except as otherwise provided. Improvements are expected in analytical techniques, and experimental programs are expected to provide increased and improved knowledge about ECCS performance. On the basis of such improvements in technology, these criteria will be modified from time to time.

The Commission believes that these criteria for emergency core cooling systems provide reasonable assurance that such systems will be effective in the unlikely event of a loss-of-coolant accident. Nevertheless, in connection with water power reactors yet to be designed and constructed the possibility of accomplishing by changes in design further improvements in the capability of emergency core cooling systems should be considered.

A. Criteria for all light-water power reactors. These general requirements have been the basis of AEC safety review for some time. On the basis of today's knowledge, the performance of the emergency core cooling system is judged to be acceptable if the calculated course of the loss-of-coolant accident¹ is limited as follows:

1. The calculated maximum fuel element cladding temperature does not exceed 2,300° F. This limit has been chosen on the basis of available data on embrittlement and possible subsequent shattering of the cladding. The results of further detailed experiments could be the basis for future revision of this limit.

2. The amount of fuel element cladding that reacts chemically with water or steam does not exceed 1 percent of the total amount of cladding in the reactor.

3. The clad temperature transient is terminated at a time when the core geometry is still amenable to cooling, and before the cladding is so embrittled as to fall during or after quenching.

4. The core temperature is reduced and decay heat is removed for an extended period of time, as required by the long-lived radioactivity remaining in the core.

B. Criteria for specific reactors. Each reactor shall be evaluated in accordance with the general criteria of section IV.A, and using a suitable evaluation model. Examples of acceptable evaluation

models are described in Appendix A.² These evaluation models are acceptable to the Commission but their use is not mandatory. Other evaluation models may be proposed by applicants for review in individual cases.

C. Application of criteria to reactor licensing—1. Application to operating reactors. (a) For each reactor holding an operating license on the effective date of these criteria and not covered by paragraph (b) below, an analysis of the performance of the emergency core cooling system presently installed, using methods equivalent to those in Appendix A, shall be submitted to the AEC as soon as practicable, but not later than October 1, 1971. Each such operating reactor shall be shown by that date to be in compliance with the criteria of sections IV A and B.

(b) For reactors granted operating licenses on or before January 1, 1968, compliance with the criteria of sections IV A and B will not be required until July 1, 1974. Each such reactor, to the extent that it is not in compliance with the criteria, shall be subject to the following additional requirements:

- (1) An analysis of the performance of the emergency core cooling system presently installed, using methods equivalent to those in Appendix A, shall be submitted to the AEC as soon as practicable, but in no case later than January 1, 1972.

- (2) A program of improvements, and a schedule for effecting them before July 1, 1974, together with supporting analysis based on an evaluation model equivalent to those in Appendix A, shall be submitted to the AEC as soon as practicable, but in no case later than July 1, 1972.

The licensee shall make, as soon as practicable, such interim improvements in operating techniques as are practical and worthwhile in improving emergency core cooling system performance or reliability.

- (3) An augmented inservice inspection program shall be inaugurated promptly covering those portions of the system piping, pumps, and valves with a nominal diameter of 4 inches or greater and for whose postulated failure the performance of the installed emergency core cooling system would not be in compliance with the criteria. The augmented program shall be based on the American Society of Mechanical Engineers' Boiler and Pressure Vessel Code, section XI, except that the frequency of inspection shall be tripled.

- (4) Equipment shall be installed as soon as practical if needed to facilitate detection of primary-system leakage by at least two different methods. The technical specifications regarding allowable rates of identified and unidentified leakage shall be reduced to the lowest practical values.

2. Variances. (a) The Commission may authorize variances from these criteria

where their application is not practicable or for other good cause.

(b) The Commission may also authorize variances from these criteria for a limited period of time to allow completion of testing programs.

(c) The application of these criteria is expected to permit normal electrical power output of all, or almost all, power reactors. However, if a limitation should result, and if an urgent short-term need for additional power occurs because of unusual or peak demand, outage of other equipment, or other similar reasons the Commission may authorize full power operation of the reactor for a limited period.

(d) Any variance authorized hereunder shall be based upon a determination of reasonable assurance that the proposed action will not adversely affect the health and safety of the public.

APPENDIX A—ACCEPTABLE EVALUATION MODELS INCLUDING THEIR CONSERVATIVE ASSUMPTIONS AND PROCEDURES

PART 1—AEC EVALUATION MODEL FOR PRESSURIZED-WATER REACTORS

Analyses should be performed for the entire break spectrum, from 0.5 ft.³ up to and including the double-ended severance of the largest pipe of the reactor coolant pressure boundary. The combination of systems used for analysis should be derived from a failure mode and effects analysis, using the single failure criterion.

The following analytical techniques should be used:

1. Thermohydraulic calculation during blowdown—IN-1321, "RELAP 3—A Computer Program for Reactor Blowdown Analysis," June 1970.

2. A suitable refill and reflood calculation from the end of blowdown onward

3. Fuel element heatup calculation—IN-1445, "THETA 1-B, A Computer Code for Nuclear Reactor Core Thermal Analysis," February 1971. Inputs from 1 and 2 will be used for this calculation.

The user of these codes should assure himself that he has reviewed available "updated memos" and is using the correct versions and choice of options within the code.

The following assumptions and procedures are to be used. Any assumptions not specified should be fully justified.

1. Core and System Nodding.

- a. RELAP—at least 3 core nodes, at least 7 nodes in the primary side of each steam generator model, and one containment node.

- b. THETA—at least 4 radial fuel nodes and one radial cladding node; at least 7 axial fluid nodes.

2. Pump Model. The pump resistance, K, used for analysis should be fully justified. The effect of pump speed upon K should be considered. The more conservative of two assumptions (locked or running) should be used for the pump during the blowdown calculation.

3. Break Characteristics—For large breaks in the range 0.6 to 1 times the total area of the double-ended break of the largest cold-leg pipe, two break models should be used. The first model should be the double-ended severance (guillotine), which assumes that there is break flow from both ends of the broken pipe, but no communication between the broken ends. The second model should assume discharge from a single node (split).

4. A break discharge coefficient (C_b) of 1 should be used for all break sizes.

5. Decay heat—The decay heat curve described in the proposed ANS Standard, with

¹ A loss-of-coolant accident is a postulated accident that results from the loss of reactor coolant at a rate in excess of the capability of the reactor coolant makeup system from breaks in the reactor coolant pressure boundary, up to and including a break equivalent in size to the double-ended rupture of the largest pipe of the reactor coolant system.

² Westinghouse Electric Corp. proposals for subatmospheric and ice condenser containments, and proposals from The Babcock and Wilcox Co. and Combustion Engineering, Inc., are under review by the AEC.

a. 20 percent allowance for uncertainty, should be used. The fraction of decay heat generated in the hot rod should be considered to be 100 percent of this value unless a smaller value is justified.

6. Time to departure from nucleate boiling—use any calculated option in the code.

7. Heat transfer after departure from nucleate boiling—use programed transition boiling correlation option.

8. Film boiling heat transfer—use Groeneveld correlation (equation 5.7 of AECL-3281, December 1969).

9. Metal-worker reaction rate—use the Baker-Just equation, with a coefficient of 1.

10. Core flow—use $0.8 \times \text{RELAP}$ smoothed flow at the junction which is entering core. If flows are opposed, use zero flow.

11. Enthalpy and pressure—use entering plenum conditions.

12. Accumulator Bypass—For cold leg breaks, all of the water injected by the accumulators prior to end-of-blowdown shall be assumed to be lost. In this context the end-of-blowdown shall be specified as the time at which zero break flow is first computed.

13. Reflood—a calculation for the reflooding heat transfer should be performed. The contaminant back pressure assumed for the analysis should not be higher than the initial pre-break pressure plus 80 percent of the increase in pressure calculated for the accident. The following items should be constraints on the calculation:

a. No steam flow should be permitted in intact loops during the time period that accumulators are injecting.

b. Core exit quality should be calculated from entering mass flow rate and nominal FLECHT heat transfer.

c. Pump resistance, K , should be calculated on the basis of a locked rotor.

d. The effects of the nitrogen gas in the accumulator, which is discharged following accumulator water discharge, should be taken into account in calculating steam flow as a function of time.

e. The pressure drop in the steam generator should be calculated with the existing fluid conditions and associated loss coefficients.

f. All effects of cold injection water, in either a hot or cold leg, on steam flow (and ΔP) should be included in the calculation.

g. The heat transfer coefficient during reflood should be derived from FLECHT data.

PART 2—GENERAL ELECTRIC EVALUATION MODEL

Analyses should be performed for the entire break spectrum, up to and including a double-ended severance of the largest pipe of the reactor coolant pressure boundary. The combinations of systems used for analysis should be derived from a failure mode and effects analysis, using the single failure criterion as indicated in Table 2-1 of the topical report "Loss-of-Coolant Accident and Emergency Core Cooling Models for General Electric Boiling Water Reactors," NEDO-10329. The analytical techniques described in NEDO-10329 and its supplement should be used with the following exceptions:

1. During the period of flow coastdown after the minimum critical heat flux ratio at the hot spot is less than one and until the top of the jet pumps uncover, the heat transfer coefficient should be calculated using the D. C. Groeneveld correlation (AECL-3281, equation 5.7).

2. During the period of lower plenum flashing until the core becomes uncovered, the heat transfer coefficient should be calculated using Groeneveld's correlation as in 1 above.

3. The heat transfer coefficients associated with rated core spray flow should correspond to those derived from experimental data, as-

suming the cladding and channel box emissivity is equal to 0.9.

4. It should be assumed that channel wetting does not occur until 60 seconds following the wetting time calculated using the Yamanouchi analysis.

5. A range of conservatively calculated peaking factors should be studied and the combination selected which results in the most severe thermal transient for the break spectrum and combinations of systems analyzed.

6. The decay heat curve described in the proposed ANS Standard, with a 20 percent allowance for uncertainty, should be used. The fraction of decay heat generated in the hot rod should be considered to be 100 percent of this value unless a smaller value is justified. The effect of voids on reactivity during the blowdown may be taken into account.

PART 3—WESTINGHOUSE EVALUATION MODEL

Analyses should be performed for the entire break spectrum, up to and including the double-ended severance of the largest pipe of the reactor coolant pressure boundary. The combination of systems used for analyses should be derived from a failure mode and effects analysis, using the single-failure criterion.

The analytical techniques to be used are described in the topical report, "Westinghouse PWR Core Behavior Following a Loss-of-Coolant Accident" WCAP-7422-L, January 1970 (Proprietary), and a supplementary proprietary Westinghouse report, "Emergency Core Cooling Performance," received June 1, 1971, and in an appropriate nonproprietary report to be furnished by Westinghouse, with the following exceptions: For breaks greater than 0.5 ft.²—

1. The break discharge coefficient, (C_D), used with the Moody discharge flow model should be equal to 1 for all break sizes.

2. The decay heat curve described in the proposed ANS Standard, with a 20 percent allowance for uncertainty, should be used. The fraction of decay heat generated in the hot rod may be considered to be 95 percent of this value.

3. For large breaks in the range 0.6 to 1 times the total area of the double-ended break of the largest cold-leg pipe, two break models should be used. The first model should be the double-ended severance (Gullotine), which assumes that there is break flow from both ends of the broken pipe, but no communication between the broken ends. The second model should assume discharge from a single node (split).

4. The time after the break for the onset of departure from nucleate boiling at the hot spot should be equal to 0.1 second.

5. For cold leg breaks, all of the water injected by the accumulators prior to end-of-blowdown shall be assumed to be lost. In this context the end-of-blowdown shall be specified as the time at which zero break flow is first computed. The containment back pressure assumed for the blowdown analysis should not be higher than the initial pre-break pressure plus 90 percent of the increase in pressure calculated for the accident under consideration.

6. The pump resistance, K , used for analysis should be fully justified. The effect of pump speed upon K should be considered. The more conservative of two assumptions (locked or running) should be used for the pump during the blowdown calculation.

7. A calculation for the reflooding heat transfer should be performed. The containment back pressure assumed for the analysis should not be higher than the initial pre-break pressure plus 80 percent of the increase

in pressure calculated for the accident under consideration.

The following items should be constraints on the calculation:

a. No steam flow should be permitted in intact loops during the time period that accumulators are injecting.

b. Core exit quality should be calculated from entering mass flow rate and nominal FLECHT heat transfer.

c. Pump resistance should be calculated on the basis of a locked rotor.

d. The effects of the nitrogen gas in the accumulator, which is discharged following accumulator water discharge, should be taken into account in calculating steam flow as a function of time.

e. The pressure drop in the steam generator should be calculated with the existing fluid conditions and associated loss coefficients.

f. All effects of cold injection water, in either a hot or cold leg, on steam flow (and ΔP) should be included in the calculation.

g. The heat transfer coefficient during reflood should be derived from FLECHT data.

In view of the public health and safety considerations discussed above, the Commission has found that the interim acceptance criteria contained herein should be promulgated without delay, that notice of proposed issuance and public procedure thereon are impracticable, and that good cause exists for making the statement of policy effective upon publication in the FEDERAL REGISTER. The Commission invites all interested persons who desire to submit written comments or suggestions for consideration in connection with the statement of policy to send them to the Secretary of the Commission, U.S. Atomic Energy Commission, Washington, D.C. 20545, Attention: Chief, Public Proceedings Branch, within 60 days after publication of this notice in the FEDERAL REGISTER. Copies of comments received may be examined at the Commission's Public Document Room, 1717 H Street, NW., Washington, D.C. The Commission will consider all such comments and suggestions with the view to possible amendments and will issue a report. Additionally, the Commission will consider holding an informal public rule making hearing on this interim policy statement.

(Sec. 161, 68 Stat. 948, 80 Stat. 383, 81 Stat. 54; 42 U.S.C. 2201, 5 U.S.C. 552, 553)

Dated at Washington, D.C., this 25th day of June 1971.

For the Atomic Energy Commission.

W. B. McCool,

Secretary of the Commission.

[FR Doc. 71-9185 Filed 6-28-71; 8:52 am]

and by Combustion Engineering, Inc., have been reviewed by the Commission, together with the conservative assumptions and procedures appropriate to each model. The amendments to the Interim Acceptance Criteria which follow add these acceptable new evaluation models as Parts 4 and 5 of Appendix A. Conforming amendments have been made in the body of the Interim Acceptance Criteria.

1. The third and fourth paragraphs of section III are amended to read as follows:

III. EVALUATION OF EMERGENCY CORE COOLING SYSTEM PERFORMANCE

Detailed technical reviews have been performed by the AEC of the computer codes currently available for predicting emergency core cooling system performance. The AEC has developed sets of suitably conservative assumptions and procedures which together with the computer codes comprise five appropriately conservative evaluation models to use for evaluation. The codes used in one of these evaluation models (described in Part 1 of Appendix A) are available from the AEC. Codes used in the other four evaluation models (described in Parts 2-5 of Appendix A) contain proprietary material, for which summaries are or soon will be publicly available. Other evaluation models are under review by the AEC.

The five acceptable evaluation models presently included in Appendix A are different in many respects, and the sets of conservative assumptions and procedures also differ from one another. These differences arise from two principal causes: (1) Differences in approach and calculational methods of the different analyses, leading to different areas where imperfect knowledge or analysis require conservative treatments, and (2) differences in hardware among the various reactor designs, such as spray vs. flood cooling and hot leg vs. cold leg vs. direct vessel injection.

2. New Parts 4 and 5 are added to Appendix A to read as follows:

APPENDIX A—ACCEPTABLE EVALUATION MODELS INCLUDING THEIR CONSERVATIVE ASSUMPTIONS AND PROCEDURES

PART 4—BABCOCK AND WILCOX EVALUATION MODEL*

Analyses should be performed for the entire break spectrum, from 0.5 ft.³, up to and including the double-ended severance of the largest pipe of the reactor coolant pressure boundary. The combination of systems used for analyses should be derived from a failure mode and effects analysis, using the single failure criterion.

The analytical techniques to be used, with the assumptions and procedures described in §§ 1.1-2.6, are those described in the following topical reports:

1. "CRAFT—Description of Model for Equilibrium LOCA Analysis Program"—Report BAW-10030, October 1971.

2. "REFLOOD—Description of Model for

* This evaluation model applies to reactors containing internal vent valves.

Multinode Core Reflood Analysis"—Report BAW-10031, October 1971.

3. "THETA 1-B, A Computer Code for Nuclear Reactor Core Thermal Analysis," Idaho Nuclear Corporation Report IN-1445, February 1971.

4. "Multinode Analysis of B&W's 2568 MWt Nuclear Plants During A Loss-of-Coolant Accident"—Report BAW-10034, October 1971.

Blowdown Period

1.1 Core and System Noding.

1.1.1 CRAFT—At least three core nodes should be used, and at least four steam generator nodes (primary side) should be used. A containment node should be used.

1.1.2 THETA 1-B—At least six radial fuel nodes and two radial clad nodes, in conjunction with at least 10 axial fuel nodes, should be used.

1.2 Pump Model.

The pump characteristics, including the effect of pump speed, for analyses should be fully justified. The more conservative of two assumptions (locked or running) should be used for the pump during the blowdown calculation.

1.3 Break Characteristics.

For large breaks in the range of 0.6-1.0 times the total area of the double-ended break of the largest cold-leg pipe, two break models should be used. The first model should be the double-ended severance (guillotine) which assumes that there is break flow from both ends of the broken pipe, but no communication between the broken ends. The second model should assume discharge from a single node (split).

1.4 Discharge Coefficient.

A break discharge coefficient $C_D = 1.0$ should be used for all break sizes.

1.5 Decay Heat.

The decay heat curve described in the proposed ANS standard,⁴ increased by a +20 percent allowance for uncertainty, should be used. The fraction of decay heat generated in the hot rod may be considered to be 0.86 times this value.

1.6 Time to Departure from Nucleate Boiling (DNB).

The time to DNB should be calculated using any one of the programmed options of the THETA 1-B code.

1.7 Film Boiling Heat Transfer.

The Groeneveld correlation (equation 5.7 of AECL-3281, December 1969) should be used in the THETA 1-B code for the film-boiling heat transfer regime.

1.8 Metal-Water Reaction Rate.

The metal-water reaction rates should be calculated using the Baker-Just equation with a coefficient of 1.0.

1.9 Core Flow Rate.

The smoothed core flow rate at the hot spot location, derived from the CRAFT code and multiplied by 0.8, should be used as input to the THETA 1-B fuel rod heatup calculation.

1.10 Enthalpy and Pressure.

The core pressure and the entering plenum enthalpy, derived from the CRAFT code, should be used as input to the THETA 1-B calculations.

1.11 Core Flooding Tank Bypass.

For cold leg breaks, all of the water injected by the core flooding tanks prior to the

CRITERIA FOR EMERGENCY CORE COOLING SYSTEMS FOR LIGHT-WATER POWER REACTORS

Interim Acceptance

On June 29, 1971, the Atomic Energy Commission published its Interim Policy Statement, "Interim Acceptance Criteria for Emergency Core Cooling Systems for Light-Water Power Reactors." (36 F.R. 12247.) The Statement included, as Appendix A, Parts 1-3, acceptable evaluation models, including conservative assumptions and procedures. Since that time, proposals for evaluation models made by The Babcock and Wilcox Co.

⁴ "Energy Release Rates Following Shutdown of Uranium-Fuel Thermal Reactors," Subcommittee ANS-5, American Nuclear Society, October 1971. Copies may be obtained from Dr. M. E. Remley, Chairman, Subcommittee ANS-5, Atomics International, Post Office Box 309, Canoga Park, CA 91305. Copies are available for public inspection at the Commission's Public Document Room, 1717 H Street NW, Washington, DC.

end-of-blowdown should be assumed to be lost. In this context the end-of-blowdown should be considered to be the time at which zero break flow is first computed.

Reflood Period

2.1 The core reflood performance should be calculated using the REFLOOD code described in BAW-10031.

2.2 An adiabatic heatup of the core should be assumed from the time of end-of-blowdown until the emergency core cooling fluid reaches the bottom of the core.

2.3 For the reflood calculation, the containment pressure should not exceed the initial prebreak pressure plus 80 percent of the increase in pressure calculated by the methods used for containment design for the accident under consideration.

2.4 The steam flow rate from the core, as it affects the Reflood pressure-drop calculations, should be calculated on the basis of core heat transfer coefficients that are equal to or greater than Flecht heat transfer coefficients. The internal vent valves should be the only flow path from the upper plenum.

2.5 The fuel rod temperature transients should be calculated on the basis of heat transfer coefficients derived from flecht.

PART 5—COMBUSTION ENGINEERING EVALUATION MODEL

Analyses should be performed for the entire break spectrum, from 0.5 ft.³ up to and including the double-ended severance of the largest pipe of the reactor coolant pressure boundary. The combination of systems used for analyses should be derived from a failure mode and effects analysis, using the single failure criterion.

The analytical techniques to be used, with the assumptions and procedures described in §§ 1.1-2.6, are those described in the following topical reports. Suitable nonproprietary reports are to be submitted.

1. "Description of Loss-of-Coolant Calculational Procedures," CENPD-26, Proprietary Combustion Engineering Report, August, 1971.

2. "Description of Loss-of-Coolant Calculational Procedures," Proprietary Combustion Engineering Report, Supplement 1 to CENPD-26, October, 1971.

3. "Steam Venting Experiments and Their Application to CE Evaluation Model," Proprietary Combustion Engineering Report, Supplement 2 to CENPD-26, November, 1971.

4. "Moisture Carry-over During PWR Post-LOCA Core Refill," Informal proprietary Combustion Engineering submittal, November, 1971.

Blowdown Period

1.1 Discharge Coefficient.

The break discharge coefficient, (C_b) used with the Moody discharge flow model should be equal to 1.0 for all break sizes.

1.2 Decay Heat.

The decay heat curve described in the proposed ANSI Standard,⁴ increased by a +20 percent allowance for uncertainty, should be used. The fraction of decay heat generated in the hot rod may be considered to be 0.94, times this value unless a smaller value is justified.

1.3 Break Characteristics.

For large breaks in the range 0.6 to 1.0 times the total area of the double-ended break of the largest cold-leg pipe, two break models should be used. The first model should be the double-ended severance (gullotine), which assumes that there is break flow from both ends of the broken pipe, but no communication between the broken ends. The second model should assume discharge from a single node (split).

1.4 Safety Injection Tank Bypass.

For cold leg breaks, all of the water injected by the safety injection tanks prior to end-of-blowdown should be assumed to be

lost. In this context the end-of-blowdown should be considered to be the time at which zero break flow is first computed.

1.5 Pump Model.

The pump characteristics, including the effect of pump speed, for analyses should be fully justified. The more conservative of two assumptions (locked or running) should be used for the pump during the blowdown calculation.

Reflood Period

2.1 The reflood sequence of events should be calculated using the analytical methods described in CENPD-26 and its supplements. The containment back pressure assumed for the analysis should not be higher than the initial prebreak pressure plus 80 percent of the increase in pressure calculated by the methods used for containment design for the accident under consideration.

2.2 All effects of cold injection water, in either a hot or cold leg, on steam flow (and ΔP) should be included in the calculation. The steam flow in intact loops during the time period that the safety injection tanks are injecting should be calculated as described in Supplement 2 of CENPD-26. The steam flow rate from the core as it affects the pressure-drop calculations should be calculated on the basis of core heat transfer coefficients that are equal to or greater than FLECHT heat transfer coefficients.

2.3 Pump resistance, K , should be calculated on the basis of a locked rotor.

2.4 The effects of the nitrogen gas in the safety injection tank which is discharged following water discharge, should be taken into account in calculating steam flow as a function of time.

2.5 The pressure drop in the steam generator should be calculated with the existing fluid conditions and associated loss coefficients.

2.6 The heat transfer coefficient for the fuel rod temperature calculations during reflood should be derived from FLECHT data.

In view of the necessity, from the standpoint of public health and safety of providing interim criteria for emergency core cooling systems applicable to all nuclear power reactors, the Commission has found that the amendments contained herein should be promulgated without delay, that notice of proposed issuance and prior public procedure are impracticable, and that good cause exists for making the amendments effective upon publication in the FEDERAL REGISTER. The Commission has issued a notice scheduling a public rule making hearing on the Interim Acceptance Criteria for Emergency Core Cooling Systems for Light Water Cooled Nuclear Power Reactors (36 F.R. 22774). The amendments herein will be considered at that hearing. Interested persons desiring to participate in that hearing should refer to that notice for the procedures available. Interested persons who desire to submit written comments or suggestions for consideration in connection with the amendments should send them to the Secretary of the Commission, U.S. Atomic Energy Commission, Washington, D.C. 20545, Attention Chief, Public Proceedings Branch, within 30 days after publication of this notice in the FEDERAL REGISTER. Copies of comments received may be examined at the Commission's Public Document Room, 1717 H Street NW., Washington, DC.

(Sec. 101, 68 Stat. 948, 80 Stat. 363, 81 Stat. 54; 42 U.S.C. 2201, 5 U.S.C. 552, 556)

Dated at Germantown, Md., this 16th day of December 1971.

For the Atomic Energy Commission.

F. T. Honns,

Acting Secretary of the Commission.

[FR Doc.71-10045 Filed 12-17-71; 10:28 am]

⁴ See footnote on page 24082.

Revised ECCS Acceptance Criteria

APPENDIX

On November 30, 1971, the Atomic Energy Commission published in the *Federal Register* (36 *F.R.* 22774) a notice scheduling a legislative-type public rule making hearing on January 27, 1972, before a hearing board consisting of Nathaniel H. Goodrich, Esq., Chairman, Dr. Lawrence R. Quarles, and Dr. John H. Buck, concerning its interim statement of policy establishing acceptance criteria for emergency core cooling systems for light water-cooled nuclear power reactors, published June 29, 1971 (36 *F.R.* 12247). Amendments to the interim criteria were published in the *Federal Register* on December 18, 1971 (36 *F.R.* 24082) in a notice that stated that the amendments would also be considered at the rule making hearing.

Participation in the rule making hearing was extensive. The primary participants included the Commission Regulatory Staff, four reactor manufacturers, a consolidated group of electric utility companies, and the Consolidated National Intervenor (CNI), a group of about 60 organizations and individuals. In addition, three states, the Lloyd Harbor Study Group, and several individuals participated to a lesser degree. The hearings lasted a total of 125 days and generated a record of more than 22,000 pages of transcript and thousands of pages of written direct testimony and exhibits. Oral argument from the seven principal participants was heard by the Commission on October 9, 1973.

In implementation of the National Environmental Policy Act of 1969, (P.L. 91-190), a Draft Environmental Statement concerning the proposed rule making was forwarded to the Council on Environmental Quality on December 6, 1972, and circulated for comment to participants in the hearing and interested Federal Agencies on December 7, 1972. Notice of public availability of the Statement and an invitation for comment was also published in the *Federal Register* at that time. Comments on the Draft Statement were received and a Final Environmental Statement was published on May 9, 1973.

The Commission noted in the interim Policy Statement:

Protection against a highly unlikely loss-of-coolant accident has long been an essential part of the defense-in-depth concept used by the nuclear power industry and the AEC to assure the safety of nuclear power plants. In this concept, the primary assurance of safety is accident prevention by correctly designing, constructing, and operating the reactor. Extensive and systematic quality assurance practices are required and applied at every step to achieve this primary assurance of safety. Nevertheless, deviations from expected behavior are postulated to occur, and protective systems are installed to take corrective action as required in such events. Notwithstanding all this, the occurrence of serious accidents is postulated, in spite of the fact that they are highly unlikely, and engineered safety features are installed to mitigate the consequences of these unlikely events. The loss-of-coolant accident is such a postulated improbable accident; the emergency core cooling system is one of the engineered safety features installed to mitigate its consequences.

The Commission has adopted new regulations, set forth below, dealing with the effectiveness of ECCS. In a 140 page opinion issued on December 28, 1973, the Commission discussed the changes from the interim acceptance criteria and the technical reason for them. Copies of this opinion are available for inspection and copying at the Commission's Public Document Room, 1717 H. Street, N. W., Washington, D. C.

The principal changes from the Interim Policy Statement are as follows. The old criterion number one, specifying that the temperature of the Zircaloy cladding should not exceed 2300°F, is replaced by two criteria, lowering the allowed peak Zircaloy temperature to 2200°F and providing a limit on the maximum allowed local oxidation. The other three criteria of the IAC are retained, with some modification of the wording. These three criteria limit the hydrogen generation from metal-water reactions, require maintenance of a coolable core geometry, and provide for long-term cooling of the quenched core.

The most important effect of the changes in the required features of the evaluation models is that swelling and bursting of the cladding must now be taken into consideration when they are calculated to occur, and that the maximum temperature and oxidation criteria must be applied to the region of clad swelling or bursting when the maximum temperature and oxidation are calculated to occur there. Another important change is the requirement that, in the steady state operation just before the postulated accident, the thermal conductance of the gap between the fuel pellets and the cladding should be calculated taking into consideration any increase in gap dimensions resulting from such phenomena as fuel densification, and should also consider the effects of the presence of fission gases. When these effects are taken into consideration a higher stored energy may be calculated. Other changes in the evaluation models are mostly in the direction of replacing previous broad conservative assumptions with more detailed calculations where new experimental information is available or where better calculational methods have been developed.

The wording of the definition of a loss-of-coolant accident has been modified to conform to its long-accepted usage, limiting it to breaks in pipes. The new regulations also require a more complete documentation of the evaluation models that are used.

The Commission believes that the implementation of the new regulations will ensure an adequate margin of performance of the ECCS should a design basis LOCA ever occur. This margin is provided by conservative features of the evaluation models and by the criteria themselves. Some of the major points that contribute to the conservative nature of the evaluations and the criteria are as follows:

(1) *Stored Heat.* The assumption of 102% of maximum power, highest allowed peaking factor, and highest estimated thermal resistance between the UO_2 and the cladding provides a calculated stored heat that is possible but unlikely to occur at the time of a hypothetical accident. While not necessarily a margin over the extreme condition, it represents at least an assumption that an accident happens at a time which is not typical.

(2) *Blow-down.* The calculation of the heat transfer during blowdown is made in a very conservative manner. There is evidence that more of the stored heat would be removed than calculated, although there is not yet an accepted way of calculating the heat transfer more accurately. It is probable that this represents a conservatism of several hundred degrees F in stored energy after blowdown, most of which can reasonably be expected to carry over to a reduction in the calculated peak temperature of the Zircaloy cladding.

(3) *Rate of Heat Generation.* It is assumed that the heat generation rate from the decay of fission products is 20% greater than the proposed ANS standard. This represents an upper limit to the degree of uncertainty. The assumption that the fission product level is that resulting from operation at 102% of rated power for an infinite time represents an improbable situation, with a conservatism that is probably in the range of 5 to 15%. The use of the Baker-Just equation for calculating the heat generation from the steam oxidation of zircaloy should also provide some conservatism, but the factor is uncertain.

(4) *The Peak Temperature Criterion.* The limitation of the peak calculated temperature of the cladding to 2200°F and the stipulation that this criterion be applied to the hottest region of the hottest fuel rod provide a substantial degree of conservatism. They ensure that the core would suffer very little damage in the accident.

Pursuant to the Atomic Energy Act of 1954, as amended, and Sections 552 and 553 of Title 5 of the United States Code, the following amendments to Title 10, Chapter I, Code of Federal Regulations, Part 50, are published as a document subject to codification to be effective on [30 days after publication in the *Federal Register*].

1. A new sentence is added to Section 50.34(a)(4) of 10 CFR Part 50 to read as follows:

§50.34 Contents of applications: technical information

(a) **

(4) *** Analysis and evaluation of ECCS cooling performance following postulated loss-of-coolant accidents shall be performed in accordance with the requirements of §50.46 for facilities for which construction permits may be issued after December 28, 1974.

2. A new sentence is added to Section 50.34(b)(4) 10 CFR Part 50 to read as follows:

§50.34 Contents of applications; technical information.

(a) ***

(b) ***

(4) *** Analysis and evaluation of ECCS cooling performance following postulated loss-of-coolant accidents shall be performed in accordance with the requirements of §50.46 for facilities for which a license to operate may be issued after December 28, 1974.

3. A new §50.46 is added to 10 C.F.R. Part 50 to read as follows:

§50.46 Acceptance Criteria for Emergency Core Cooling Systems for Light Water Nuclear Power Reactors.

(a)(1) Except as provided in subparagraphs (2) and (3) of this paragraph, each boiling and pressurized light-water nuclear power reactor fueled with uranium oxide pellets within cylindrical zircaloy cladding shall be provided with an emergency core cooling system (ECCS) which shall be designed such that its calculated cooling performance following postulated loss-of-coolant accidents conforms to the criteria set forth in paragraph (b). ECCS cooling performance shall be calculated in accordance with an acceptable evaluation model, and shall be calculated for a number of postulated loss-of-coolant accidents of different sizes, locations, and other properties sufficient to provide assurance that the entire spectrum of postulated loss-of-coolant accidents is covered. Appendix K, ECCS Evaluation Models, sets forth certain required and acceptable features of evaluation models. Conformance with the criteria set forth in paragraph (b), with ECCS cooling performance calculated in accordance with an acceptable evaluation model, may require that restrictions be imposed on reactor operation.

(2) With respect to reactors for which operating licenses have previously been issued and for which operating licenses may issue on or before December 28, 1974:

(i) The time within which actions required or permitted under this subparagraph (2) must occur shall begin to run on [30 days after publication of the rule in the *Federal Register*].

(ii) Within six months following the date specified in subparagraph (i) of this subparagraph (2), an evaluation in accordance with subparagraph (1) of this paragraph (a) shall be submitted to the Director of Regulation. The evaluation shall be accompanied by such proposed changes in technical specifications or license amendments as may be necessary to bring reactor operation in conformity with subparagraph (1) of this paragraph.

(iii) Any licensee may request an extension of the six-month period referred to in subparagraph (ii) of this subparagraph (2) for good cause. Any such request shall be submitted not less than 45 days prior to expiration of the six-month period, and shall be accompanied by affidavits showing precisely why the evaluation is not complete and the minimum time believed necessary to complete it. The Director of Regulation shall cause notice of such a request to be published promptly in the *Federal Register*; such notice shall provide for the submission of comments by interested persons within a time period to be established by the Director of Regulation. If, upon reviewing the foregoing submissions, the Director of Regulation concludes that good cause has been shown for an extension, he may extend the six-month period for the shortest additional time which in this judgment will be necessary to enable the licensee to furnish the submissions required by subparagraph (ii) of this subparagraph (2). Requests for extensions of the six-month period, submitted under this subparagraph, shall be ruled upon by the Director of Regulation prior to expiration of that period.

(iv) Upon submission of the evaluation required by subparagraph (ii) of this subparagraph (2) (or under subparagraph (iii), if the six-month period is extended) the facility shall continue or commence operation only within the limits of both the proposed technical specifications or license amendments submitted in accordance with this subparagraph (2) and all technical specifications or license conditions previously imposed by the Commission, including the requirements of the Interim Policy Statement (June 29, 1971, 36 *F.R.* 12248), as amended (December 18, 1971, 36 *F.R.* 24082).

(v) Further restrictions on reactor operation will be imposed by the Director of Regulation if he finds that the evaluations submitted under subparagraphs (ii) and (iii) of this subparagraph (2) are not consistent with subparagraph (1) of this paragraph (a) and as a result such restrictions are required to protect the public health and safety.

(vi) Exemptions from the operating requirements of subparagraph (iv) of this subparagraph (2) may be granted by the Commission for good cause. Requests for such exemption shall be submitted not less than 45 days prior to the date upon which the plant would otherwise be required to operate in accordance with the procedures of said subparagraph (iv). Any such request shall be filed with the Secretary of the Commission, who shall cause notice of its receipt to be published promptly in the *Federal Register*; such notice shall provide for the submission of comments by interested persons within 14 days following *Federal Register* publication. The Director of Regulation shall submit his views as to any requested exemption within five days following expiration of the comment period.

(vii) Any request for an exemption submitted under subparagraph (vi) of this subparagraph (2) must show, with appropriate affidavits and technical submissions, that it would be in the public interest to allow the licensee a specified additional period of time within which to alter the operation of the facility in the manner required by subparagraph (iv) of this subparagraph (2). The request shall also include a discussion of the alternatives available for establishing compliance with the rule.

(3) Construction permits may be issued after December 28, 1973 but before December 28, 1974 subject to any applicable conditions or restrictions imposed pursuant to other regulations in this chapter and the Interim Acceptance Criteria for Emergency Core Cooling Systems published on June 29, 1971 (36 F.R. 12248) as amended (December 18, 1971, 36 F.R. 24082): *Provided, however*, that no operating license shall be issued for facilities constructed in accordance with construction permits issued pursuant to this subparagraph, unless the Commission determines, among other things, that the proposed facility meets the requirements of subparagraph (1) of this paragraph.

(b)(1) *Peak Cladding Temperature.* The calculated maximum fuel element cladding temperature shall not exceed 2200°F.

(2) *Maximum Cladding Oxidation.* The calculated total oxidation of the cladding shall nowhere exceed 0.17 times the total cladding thickness before oxidation. As used in this subparagraph total oxidation means the total thickness of cladding metal that would be locally converted to oxide if all the oxygen absorbed by and reacted with the cladding locally were converted to stoichiometric zirconium dioxide. If cladding rupture is calculated to occur, the inside surfaces of the cladding shall be included in the oxidation, beginning at the calculated time of rupture. Cladding thickness before oxidation means the radial distance from inside to outside the cladding, after any calculated rupture or swelling has occurred but before significant oxidation. Where the calculated conditions of transient pressure and temperature lead to a prediction of cladding swelling, with or without cladding rupture, the unoxidized cladding thickness shall be defined as the cladding cross-sectional area, taken at a horizontal plane at the elevation of the rupture, if it occurs, or at the elevation of the highest cladding temperature if no rupture is calculated to occur, divided by the average circumference at that elevation. For ruptured cladding the circumference does not include the rupture opening.

(3) *Maximum Hydrogen Generation.* The calculated total amount of hydrogen generated from the chemical reaction of the cladding with water or steam shall not exceed 0.01 times the hypothetical amount that would be generated if all of the metal in the cladding cylinders surrounding the fuel, excluding the cladding surrounding the plenum volume, were to react.

(4) *Coolable Geometry.* Calculated changes in core geometry shall be such that the core remains amenable to cooling.

(5) *Long-Term Cooling.* After any calculated successful initial operation of the ECCS, the calculated core temperature shall be maintained at an acceptably low value and decay heat shall be removed for the extended period of time required by the long-lived radioactivity remaining in the core.

(c) As used in this section:

(1) Loss-of-coolant accidents (LOCA's) are hypothetical accidents that would result from the loss of reactor coolant, at a rate in excess of the capability of the reactor coolant makeup system, from breaks in pipes in the reactor coolant pressure boundary up to and including a break equivalent in size to the double-ended rupture of the largest pipe in the reactor coolant system.

(2) An evaluation model is the calculational framework for evaluating the behavior of the reactor system during a postulated loss-of-coolant accident (LOCA). It includes one or more computer programs and all other information necessary for application of the calculational framework to a specific LOCA, such as mathematical models used, assumptions included in the programs, procedure for treating the program input and output information, specification of those portions of analysis not included in computer programs, values of parameters, and all other information necessary to specify the calculational procedure.

(d) The requirements of this section are in addition to any other requirements applicable to ECCS set forth in this Part. The criteria set forth in paragraph (b), with cooling performance calculated in accordance with an acceptable evaluation model, are in implementation of the general requirements with respect to ECCS cooling performance design set forth in this Part, including in particular Criterion 35 of Appendix A.

4. A new Appendix K is added to 10 CFR Part 50 to read as follows: Appendix K- ECCS Evaluation Models.

I. Required and Acceptable Features of Evaluation Models.

II. Required Documentation.

I. REQUIRED AND ACCEPTABLE FEATURES OF THE EVALUATION MODELS

A. SOURCES OF HEAT DURING THE LOCA

For the heat sources listed in Paragraphs 1 to 4 below it shall be assumed that the reactor has been operating continuously at a power level at least 1.02 times the licensed power level (to allow for such uncertainties as instrumentation error), with the maximum peaking factor allowed by the technical specifications. A range of power distribution shapes and peaking factors representing power distributions that may occur over the core lifetime shall be studied and the one selected should be that which results in the most severe calculated consequences, for the spectrum of postulated breaks and single failures analyzed.

1. *The Initial Stored Energy in the Fuel.* The steady-state temperature distribution and stored energy in the fuel before the hypothetical accident shall be calculated for the burn-up that yields the highest calculated cladding temperature (or, optionally, the highest calculated stored energy). To accomplish this, the thermal conductivity of the UO_2 shall be evaluated as a function of burn-up and temperature, taking into consideration differences in initial density, and the thermal conductance of the gap between the UO_2 and the cladding shall be evaluated as a function of the burn-up, taking into consideration fuel densification and expansion, the composition and pressure of the gases within the fuel rod, the initial cold gap dimension with its tolerances, and cladding creep.

2. *Fission Heat.* Fission heat shall be calculated using reactivity and reactor kinetics. Shutdown reactivities resulting from temperatures and voids shall be given their minimum plausible values, including allowance for uncertainties, for the range of power distribution shapes and peaking factors indicated to be studied above. Rod trip and insertion may be assumed if they are calculated to occur.

3. *Decay of Actinides.* The heat from the radioactive decay of actinides, including neptunium and plutonium generated during operation, as well as isotopes of uranium, shall be calculated in accordance with fuel cycle calculations and known radioactive properties. The actinide decay heat chosen shall be the appropriate for the time in the fuel cycle that yields the highest calculated fuel temperature during the LOCA.

4. *Fission Product Decay.* The heat generation rates from radioactive decay of fission products shall be assumed to be equal to 1.2 times the values for infinite operating time in the ANS Standard (Proposed American Nuclear Society Standard—“Decay Energy Release Rates Following Shutdown of Uranium-Fueled Thermal Reactors”, Approved by Subcommittee ANS-5, ANS Standards Committee, October 1971). The fraction of the locally generated gamma energy that is deposited in the fuel (including the cladding) may be different from 1.0; the value used shall be justified by a suitable calculation.

5. *Metal-Water Reaction Rate.* The rate of energy release, hydrogen generation, and cladding oxidation from the metal/water reaction shall be calculated using the Baker-Just equation (Baker, L., Just, L.C., “Studies of Metal Water Reactions at High Temperatures, III. Experimental and Theoretical Studies of the Zirconium-Water Reaction,” ANL-6548, page 7, May 1962). The reaction shall be assumed not to be steam limited. For rods whose cladding is calculated to rupture during the LOCA, the inside of the cladding shall also be assumed to react after the rupture. The calculation of the reaction rate on the inside of the cladding shall also follow the Baker-Just equation, starting at the time when the cladding is calculated to rupture and extending around the cladding inner circumference and axially no less than 1.5 inches each way from the location of the rupture, with the reaction assumed not to be steam limited.

6. *Reactor Internals Heat Transfer.* Heat transfer from piping, vessel walls, and non-fuel internal hardware shall be taken into account.

7. *Pressurized Water Reactor Primary-to-Secondary Heat Transfer.* Heat transferred between primary and secondary systems through heat exchangers (steam generators) shall be taken into account. (Not applicable to Boiling Water Reactors.)

B. SWELLING AND RUPTURE OF THE CLADDING AND FUEL ROD THERMAL PARAMETERS

Each evaluation model shall include a provision for predicting cladding swelling and rupture from consideration of the axial temperature distribution of the cladding and from the difference in pressure between the inside and outside of the cladding, both as functions of time. To be acceptable the swelling and rupture calculations shall be based on applicable data in such a way that the degree of swelling and incidence of rupture are not underestimated. The degree of swelling and rupture shall be taken into account in calculations of gap conductance, cladding oxidation and embrittlement, and hydrogen generation.

The calculations of fuel and cladding temperatures as a function of time shall use values for gap conductance and other thermal parameters as functions of temperature and other applicable time-dependent variables. The gap conductance shall be varied in accordance with changes in gap dimensions and any other applicable variables.

C. BLOWDOWN PHENOMENA

1. Break Characteristics and Flow

a. In analyses of hypothetical loss-of-coolant accidents, a spectrum of possible pipe breaks shall be considered. This spectrum shall include instantaneous double-ended breaks ranging in cross-sectional area up to and including that of the largest pipe in the primary coolant system. The analysis shall also include the effects of longitudinal splits in the largest pipes, with the split area equal to the cross-sectional area of the pipe.

b. *Discharge Model.* For all times after the discharging fluid has been calculated to be two-phase in composition, the discharge rate shall be calculated by use of the Moody model (F. J. Moody, "Maximum Flow Rate of a Single Component, Two-Phase Mixture," *Journal of Heat Transfer, Transactions of the American Society of Mechanical Engineers*, 87, No. 1, February 1965). The calculation shall be conducted with at least three values of a discharge coefficient applied to the postulated break area, these values spanning the range from 0.6 to 1.0. If the results indicate that the maximum clad temperature for the hypothetical accident is to be found at an even lower value of the discharge coefficient, the range of discharge coefficients shall be extended until the maximum clad temperature calculated by this variation has been achieved.

c. *End of Blowdown.* (Applies Only to Pressurized Water Reactors.) For postulated cold leg breaks, all emergency cooling water injected into the inlet lines or the reactor vessel during the bypass period shall in the calculations be subtracted from the reactor vessel calculated inventory. This may be executed in the calculation during the bypass period, or as an alternative the amount of emergency core cooling water calculated to be injected during the bypass period may be subtracted later in the calculation from the water remaining in the inlet lines, downcomer, and reactor vessel lower plenum after the bypass period. This bypassing shall end in the calculation at a time designated as the "end of bypass," after which the expulsion or entrainment mechanisms responsible for the bypassing are calculated not to be effective. The end-of-bypass definition used in the calculation shall be justified by a suitable combination of analysis and experimental data. Acceptable methods for defining "end of bypass" include, but are not limited to, the following: (1) Prediction of the blowdown calculation of downward flow in the downcomer for the remainder of the blowdown period; (2) Prediction of a threshold for droplet entrainment in the upward velocity, using local fluid conditions and a conservative critical Weber number.

d. *Noding Near the Break and the ECCS Injection Points.* The noding in the vicinity of and including the broken or split sections of pipe and the points of ECCS injection shall be chosen to permit a reliable analysis of the thermodynamic history in these regions during blowdown.

2. **Frictional Pressure Drops.** The frictional losses in pipes and other components including the reactor core shall be calculated using models that include realistic variation of friction factor with Reynolds number, and realistic two-phase friction multipliers that have been adequately verified by comparison with experimental data, or models that prove at least equally conservative with respect to maximum clad temperature calculated during the hypothetical accident. The modified Baroczy correlation (Baroczy, C. J., "A Systematic Correlation for Two-Phase Pressure Drop," *Chem. Enging. Prog. Symp. Series*, No. 64, Vol. 62, 1965) or a combination of the Thom correlation (Thom, J. R. S., "Prediction of Pressure Drop During Forced Circulation Boiling of Water," *Int. J. of Heat & Mass Transfer*, 7, 709-724, 1964) for pressures equal to or greater than 250 psia and the Martinelli-Nelson correlation (Martinelli, R. C., Nelson, D. B., "Prediction of Pressure Drop During Forced Circulation Boiling of Water," *Transactions of ASME*, 695-702, 1948) for pressures lower than 250 psia is acceptable as a basis for calculating realistic two-phase friction multipliers.

3. **Momentum Equation.** The following effects shall be taken into account in the conservation of momentum equation: (1) temporal change of momentum, (2) momentum convection, (3) area change momentum flux, (4) momentum change due to compressibility, (5) pressure loss resulting from wall friction, (6) pressure loss resulting from area change, and (7) gravitational acceleration. Any omission of one or more of these terms under stated circumstances shall be justified by comparative analyses or by experimental data.

4. Critical Heat Flux

a. Correlations developed from appropriate steady-state and transient-state experimental data are acceptable for use in predicting the critical heat flux (CHF) during LOCA transients. The computer programs in which these correlations are used shall contain suitable checks to assure that the physical parameters are within the range of parameters specified for use of the correlations by their respective authors.

b. Steady-state CHF correlations acceptable for use in LOCA transients include, but are not limited to, the following:

(1) *W-3*. L. S. Tong, "Prediction of Departure from Nucleate Boiling for an Axially Non-uniform Heat Flux Distribution," *Journal of Nuclear Energy*, Vol. 21, 241-248, 1967.

(2) *B&W-2*. J. S. Gellerstedt, R. A. Lee, W. J. Oberjohn, R. H. Wilson, L. J. Stanek, "Correlation of Critical Heat Flux in a Bundle Cooled by Pressurized Water," *Two-Phase Flow and Heat Transfer in Rod Bundles*, ASME, New York, 1969.

(3) *Hench-Levy*. J. M. Healzer, J. E. Hench, E. Janssen, S. Levy, "Design Basis for Critical Heat Flux Condition in Boiling Water Reactors," APED-5186, GE Company Private report, July 1966.

(4) *Macbeth*. R. V. Macbeth, "An Appraisal of Forced Convection Burnout Data," *Proceedings of the Institute of Mechanical Engineers*, 1965-1966.

(5) *Barnett*. P. G. Barnett, "A Correlation of Burnout Data for Uniformly Heated Annuli and Its Uses for Predicting Burnout in Uniformly Heated Rod Bundles," AEEW-R 463, 1966.

(6) *Hughes*. E. D. Hughes, "A Correlation of Rod Bundle Critical Heat Flux for Water in the Pressure Range 150 to 725 psia," IN-1412, Idaho Nuclear Corporation, July 1970.

c. Correlations of appropriate transient CHF data may be accepted for use in LOCA transient analyses if comparisons between the data and the correlations are provided to demonstrate that the correlations predict values of CHF which allow for uncertainty in the experimental data throughout the range of parameters for which the correlations are to be used. Where appropriate, the comparisons shall use statistical uncertainty analysis of the data to demonstrate the conservatism of the transient correlation.

d. Transient CHF correlations acceptable for use in LOCA transients include, but are not limited to, the following:

(1) *GE Transient CHF*. B. C. Slifer, J. E. Hench, "Loss-of-Coolant Accident and Emergency Core Cooling Models for General Electric Boiling Water Reactors," NEDO-10329, General Electric Company, Equation C-32, April 1971.

e. After CHF is first predicted at an axial fuel rod location during blowdown, the calculation shall not use nucleate boiling heat transfer correlations at that location subsequently during the blowdown even if the calculated local fluid and surface conditions would apparently justify the reestablishment of nucleate boiling. Heat transfer assumptions characteristic of return to nucleate boiling (rewetting) shall be permitted when justified by the calculated local fluid and surface conditions during the reflood portion of a LOCA.

5. Post-CHF Heat Transfer Correlations

a. Correlations of heat transfer from the fuel cladding to the surrounding fluid in the post-CHF regimes of transition and film boiling shall be compared to applicable steady-state and transient-state data using statistical correlation and uncertainty analyses. Such comparison shall demonstrate that the correlations predict values of heat transfer coefficient equal to or less than the mean value of the applicable experimental heat transfer data throughout the range of parameters for which the correlations are to be used. The comparisons shall quantify the relation of the correlations to the statistical uncertainty of the applicable data.

b. The Groeneveld flow film boiling correlation (Equation 5.7 of D. C. Groeneveld, "An Investigation of Heat Transfer in the Liquid Deficient Regime," AECL-3281, revised December 1969), the Dougall-Rohsenow flow film boiling correlation (R. S. Dougall and W. M. Rohsenow, "Film Boiling on the Inside of Vertical Tubes with Upward Flow of the Fluid at Low Qualities," MIT Report Number 9079-26, Cambridge, Massachusetts, September 1963), and the Westinghouse correlation of steady-state transition boiling ("Proprietary Redirect/Rebuttal Testimony of Westinghouse Electric Corporation," U.S.A.E.C. Docket RM-50-1, page 25-1, October 26, 1972) are acceptable for use in the post-CHF boiling regimes. In addition the transition boiling correlation of McDonough, Milich, and King (J. B. McDonough, W. Milich, E. C. King, "Partial Film Boiling with Water at 2000 psig in a Round Vertical Tube," MSA Research Corp., Technical Report 62 (NP-6976), (1958) is suitable for use between nucleate and film boiling. Use of all these correlations shall be restricted as follows:

(1) The Groeneveld correlation shall not be used in the region near its low-pressure singularity.

(2) the first term (nucleate) of the Westinghouse correlation and the entire McDonough, Milich, and King correlation shall not be used during the blowdown after the temperature difference between the clad and the saturated fluid first exceeds 300°F.

(3) transition boiling heat transfer shall not be reapplied for the remainder of the LOCA blowdown, even if the clad superheat returns below 300°F, except for the reflood portion of the LOCA when justified by the calculated local fluid and surface conditions.

6. Pump Modeling. The characteristics of rotating primary system pumps (axial flow, turbine, or centrifugal) shall be derived from a dynamic model that includes momentum transfer between the fluid and the rotating member, with variable pump speed as a function of time. The pump model resistance used for analysis should be justified. The pump model for the two-phase region shall be verified by applicable two-phase pump performance data. For BWR's after saturation is calculated at the pump suction, the pump head may be assumed to vary linearly with quality, going to zero for one percent quality at the pump suction, so long as the analysis shows that core flow stops before the quality at pump suction reaches one percent.

7. Core Flow Distribution During Blowdown. (Applies only to pressurized water reactors.)

a. The flow rate through the hot region of the core during blowdown shall be calculated as a function of time. For the purpose of these calculations the hot region chosen shall not be greater than the size of one fuel assembly. Calculations of average flow and flow in the hot region shall take into account cross flow between regions and any flow blockage calculated to occur during blowdown as a result of cladding swelling or rupture. The calculated flow shall be smoothed to eliminate any calculated rapid oscillations (period less than 0.1 seconds).

b. A method shall be specified for determining the enthalpy to be used as input data to the hot channel heatup analysis from quantities calculated in the blowdown analysis, consistent with the flow distribution calculations.

D. POST-BLOWDOWN PHENOMENA; HEAT REMOVAL BY THE ECCS

1. *Single Failure Criterion.* An analysis of possible failure modes of ECCS equipment and of their effects on ECCS performance must be made. In carrying out the accident evaluation the combination of ECCS subsystems assumed to be operative shall be those available after the most damaging single failure of ECCS equipment has taken place.

2. *Containment Pressure.* The containment pressure used for evaluating cooling effectiveness during reflood and spray cooling shall not exceed a pressure calculated conservatively for this purpose. The calculation shall include the effects of operation of all installed pressure-reducing systems and processes.

3. *Calculation of Reflood Rate for Pressurized Water Reactors.* The refilling of the reactor vessel and the time and rate of reflooding of the core shall be calculated by an acceptable model that takes into consideration the thermal and hydraulic characteristics of the core and of the reactor system. The primary system coolant pumps shall be assumed to have locked impellers if this assumption leads to the maximum calculated cladding temperature; otherwise the pump rotor shall be assumed to be running free. The ratio of the total fluid flow at the core exit plane to the total liquid flow at the core inlet plane (carryover fraction) shall be used to determine the core exit flow and shall be determined in accordance with applicable experimental data (for example, "PWR FLECHT (Full Length Emergency Cooling Heat Transfer) Final Report," Westinghouse Report WCAP-7665, April 1971; "PWR Full Length Emergency Cooling Heat Transfer (FLECHT) Group I Test Report," Westinghouse Report WCAP-7435, January 1970; "PWR FLECHT (Full Length Emergency Cooling Heat Transfer) Group II Test Report," Westinghouse Report WCAP-7544, September 1970; "PWR FLECHT Final Report Supplement," Westinghouse Report WCAP-7931, October 1972).

The effects on reflooding rate of the compressed gas in the accumulator which is discharged following accumulator water discharge shall also be taken into account.

4. *Steam Interaction with Emergency Core Cooling Water in Pressurized Water Reactors.* The thermal-hydraulic interaction between steam and all emergency core cooling water shall be taken into account in calculating the core reflooding rate. During refill and reflood, the calculated steam flow in unbroken reactor coolant pipes shall be taken to be zero during the time that accumulators are discharging water into those pipes unless experimental evidence is available regarding the realistic thermal-hydraulic interaction between the steam and the liquid. In this case, the experimental data may be used to support an alternate assumption.

5. *Refill and Reflood Heat Transfer for Pressurized Water Reactors.* For reflood rates of one inch per second or higher, reflood heat transfer coefficients shall be based on applicable experimental data for unblocked cores including FLECHT results ("PWR FLECHT (Full Length Emergency Cooling Heat Transfer) Final Report," Westinghouse Report WCAP-7665, April 1971). The use of a correlation derived from FLECHT data shall be demonstrated to be conservative for the transient to which it is applied; presently available FLECHT heat transfer correlations ("PWR Full Length Emergency Cooling Heat Transfer (FLECHT) Group 1 Test Report," Westinghouse Report WCAP-7544, September 1970; "PWR FLECHT Final Report Supplement," Westinghouse Report WCAP-7931, October 1972) are not acceptable. New correlations or modifications to the FLECHT heat transfer correlations are acceptable only after they are demonstrated to be conservative, by comparison with FLECHT data, for a range of parameters consistent with the transient to which they are applied.

During refill and during reflood when reflood rates are less than one inch per second, heat transfer calculations shall be based on the assumption that cooling is only by steam, and shall take into account any flow blockage calculated to occur as a result of cladding swelling or rupture as such blockage might affect both local steam flow and heat transfer.

6. *Convective Heat Transfer Coefficients for Boiling Water Reactor Fuel Rods Under Spray Cooling.* Following the blowdown period, convective heat transfer shall be calculated using coefficients based on appropriate experimental data. For reactors with jet pumps and having fuel rods in a 7×7 fuel assembly array, the following convective coefficients are acceptable:

a. During the period following lower plenum flashing but prior to the core spray reaching rated flow, a convective heat transfer coefficient of zero shall be applied to all fuel rods.

b. During the period after core spray reaches rated flow but prior to reflooding, convective heat transfer coefficients of 3.0, 3.5, 1.5, and 1.5 $\text{Btu}\cdot\text{hr}^{-1}\cdot\text{ft}^2\cdot^{\circ}\text{F}^{-1}$ shall be applied to the fuel rods in the outer corners, outer row, next to outer row, and to those remaining in the interior, respectively, of the assembly.

c. After the two-phase reflooding fluid reaches the level under consideration, a convective heat transfer coefficient of 25 $\text{Btu}\cdot\text{hr}^{-1}\cdot\text{ft}^2\cdot^{\circ}\text{F}^{-1}$ shall be applied to all fuel rods.

7. *The Boiling Water Reactor Channel Box Under Spray Cooling.* Following the blowdown period, heat transfer from, and wetting of, the channel box shall be based on appropriate experimental data. For reactors with jet pumps and fuel rods in a 7×7 fuel assembly array, the following heat transfer coefficients and wetting time correlation are acceptable.

a. During the period after lower plenum flashing, but prior to core spray reaching rated flow, a convective coefficient of zero shall be applied to the fuel assembly channel box.

b. During the period after core spray reaches rated flow, but prior to wetting of the channel, a convective heat transfer coefficient of 5 $\text{Btu}\cdot\text{hr}^{-1}\cdot\text{ft}^2\cdot^{\circ}\text{F}^{-1}$ shall be applied to both sides of the channel box.

c. Wetting of the channel box shall be assumed to occur 60 seconds after the time determined using the correlation based on the Yamanouchi analysis ("Loss-of-Coolant Accident and Emergency Core Cooling Models for General Electric Boiling Water Reactors," General Electric Company Report NEDO-10329, April 1971).

II. REQUIRED DOCUMENTATION

1. a. A description of each evaluation model shall be furnished. The description shall be sufficiently complete to permit technical review of the analytical approach including the equations used, their approximations in difference form, the assumptions made, and the values of all parameters or the procedure for their selection, as for example, in accordance with a specified physical law or empirical correlation.

b. The description shall be sufficiently detailed and specific to require significant changes in the evaluation model to be specified in amendments of the description. For this purpose, a significant change is a change that would result in a calculated fuel cladding temperature different by more than 20°F from the temperature calculated (as a function of time) for a postulated LOCA using the last previously accepted model.

c. A complete listing of each computer program, in the same form as used in the evaluation model, shall be furnished to the Atomic Energy Commission.

2. For each computer program, solution convergence shall be demonstrated by studies of system modeling or noding and calculational time steps.

3. Appropriate sensitivity studies shall be performed for each evaluation model, to evaluate the effect on the calculated results of variations in noding, phenomena assumed in the calculation to predominate, including pump operation or locking, and values of parameters over their applicable ranges. For items to which results are shown to be sensitive, the choices made shall be justified.

4. To the extent practicable, predictions of the evaluation model, or portions thereof, shall be compared with applicable experimental information.

5. General Standards for Acceptability. Elements of evaluation models reviewed will include technical adequacy of the calculational methods, including compliance with required features of Section I of this Appendix K and provision of a level of safety and margin of conservatism comparable to other acceptable evaluation models, taking into account significant differences in the reactors to which they apply.

Appendix 4 Ongoing and Planned R & D Related to LOCA-ECC in Water Cooled Reactors

TABLE II

ONGOING AND PLANNED RESEARCH AND DEVELOPMENT RELATED TO LOCA-ECC IN WATER COOLED REACTORS

PROGRAM	CODE	PURPOSE	EXPERIMENTAL SYSTEM	SCHEDULE	REF.	REMARKS
<u>Fundamental Studies in Blowdown and Heat Transfer</u>						
1. PWR blowdown heat transfer (USAEC, ORNL)	ORNL/BDHT	Obtain data on blowdown heat transfer prior to ECC injection and on heat transfer related to operational upsets.	7 x 7 electrically heated rod array 12-ft-long, profiled power distribution, 1975 PWR pin diameter and pitch.	Continuing to July 1975		Emphasize separate effects tests on blowdown heat transfer in which response of the rod array to specific initial and boundary conditions (for example, reverse and re-reverse flow) will be determined without influence of the primary system loop.
2. Investigation of Pressure Drops and Heat Transfer Coefficients (USAEC, University of Cincinnati)	U. Cin/AEC	Investigate pressure drops and heat transfer phenomena of importance during LOCA.	Two laboratory scale test systems: (1) small piping test section using freon to obtain two-phase pressure drops and flow patterns, and (2) a single tube with mercury on the inside to obtain basic transition boiling and reflooding heat transfer data.	Continuing		Fundamental studies to include conditions under which nucleate boiling is reestablished.

TABLE II (Contd.)

ONGOING AND PLANNED RESEARCH AND DEVELOPMENT RELATED TO LOCA-ECC IN WATER COOLED REACTORS

PROGRAM	CODE	PURPOSE	EXPERIMENTAL SYSTEM	SCHEDULE	REF.	REMARKS
3. Deficient Cooling (US-AEC, General Electric Company, San Jose, Calif.)	GE/DC/AEC	Determine CHF and temperature regimes occurring due to power, flow, or pressure transient upset; evaluate temperature regimes in LOCA; evaluate consequences of fuel rod geometry changes.	Single-rod and 9(or 15) rod array transient CHF tests; single-rod simulated swelling effects on steady state CHF; 9-rod array with geometry changes on spacer components and simulated bowing and swelling to investigate steady-state CHF.	Continuing	30	Transient CHF tests include transient flow, power, and pressure testing and study of effects of combinations of intra-bundle clearance, power distribution, and spacer components.
4. Blowdown of Long Straight Pipes (Risley, UKAEA)	UKAEA	Acquire basic decompression data for model development.	Long straight pipes up to 8 in. in diameter, 12 ft in length, heavily instrumented.	Continuing	31	Studies for determining basic bubble growth phenomena which cause and control critical flow.
5. Research on Safety Assessment (ROSA) (Japan Atomic Energy Research Institute, Tokai Laboratory)	ROSA	Measure pressure and leak flow transients during blowdown, obtain scoping data on DNB during blowdown, evaluate pressure oscillations.	ROSA I: 56-cm-ID and 708-cm-long pressure vessel, rod array typical of BWR fuel assembly with up to 13 electrically heated rods, rupture disc unit.	Continuing	32	BWR vessel system blowdown and heat transfer data as part of this program. A ROSA II facility is being fabricated and is described under scaled system effects experiments.
6. Heat Transfer During Blowdown (AEG-Telefunken, German Federal Ministry)	AEG/GFM	Acquire data base for heat transfer coefficients and thermal-hydraulic behavior during blowdown.	Parametric tests with an internally cooled tube 3 m in length. Test section 4-rod bundle tests using BWR and PWR power profiles and 36-rod bundle tests with BWR and PWR power profiles.	Continuing	33	Parametric tests at LOCA conditions for development of heat transfer correlations.
7. Depressurization Dynamics and Heating Transient, Fundamental Two-Phase Flow Studies (EURATOM, Ispra)	Ispra	Obtain data on heat transfer during system decompression.	Test loop consisted of an electrically-heated round-tube test section (15-mm OD, 13.8-mm ID, and 3-m length), an upper plenum, a lower plenum, a recirculation line, a pressurizer, and a quick opening valve.	---	34	One subchannel simulation.
8. Investigation of the Phenomena Involved in the Depressurization of Water-Cooled Reactors (Battelle-Institute Frankfurt, Germany)	BMI/GFM	Determine loads on vessel internals during depressurization, investigate phenomena in the initial phase of depressurization (in particular the discharge rate).	Tests utilize a vessel of 0.8-m ID and 11.2-m length with simplified internals representation. Exit pipe diameter of 150 mm.	---	35	Both BWR and PWR initial fluid conditions to be investigated.

TABLE II (Contd.)

ONGOING AND PLANNED RESEARCH AND DEVELOPMENT RELATED TO LOCA-ECC IN WATER COOLED REACTORS

PROGRAM	CODE	PURPOSE	EXPERIMENTAL SYSTEM	SCHEDULE	REF.	REMARKS
9. Bottom Flooding Experiments (CISE, Milan, Italy)	ECC/CISE	Preliminary experimental investigation of coolant channel thermal transient following bottom ECC injection.	Test facility of directly heated tubular and annular test elements 4 m in length.	---	36	Parameters varied were pressure, flow rate, coolant inlet enthalpy, heating power, and initial wall temperature. Data analyzed to determine influence of test parameters on temperature turnaround and quenching times.
10. Dryout Experiments (CISE, Milan, Italy)	BDHT/CISE	Develop a model of local and instantaneous values of flow and quality along fuel channel, compare transient dryout conditions with steady-state conditions.	Externally heated annulus, 1.35-mm ID, 2.1-cm OD and 400-cm length, with initial pressures to 50 bars.	---	37	Experimental conditions: steady-state, inlet flow stoppage with pressure transient or power surge.
11. Cooling of High-Temperature Surfaces (Central Electricity Generating Board, United Kingdom)	CEGB	Obtain data on the direct injection of coolant onto the heated surface for development of physical models.	Pin cluster tests with a concentric ring of heaters with a central sparge tube, single rod and tube experiments with water directed at top of heated length, and flat plates experiments with water directed at surface of plate.	Continuing	38	Studies on water distribution in a hot cluster and cooling effectiveness of aerosols are in progress.
<u>EMERGENCY CORE COOLANT DELIVERY</u>						
1. Semiscale - Plexiglass Vessel (USAEC, Aerojet Nuclear Company, NRTS)	P-SSBD	Provide an understanding of ECC delivery phenomena, investigate application of air-water countercurrent flow correlations to downcomer annulus geometries.	Plexiglass test vessel with geometry similar to the 1-1/2-loop semiscale vessel annulus and downcomer.	Continuing		Work related to other semiscale testing to understand ECC delivery and bypass. Effects of downcomer length, and lower plenum geometry studied.
2. Semiscale Vessel - Steady-State Tests (USAEC, Aerojet Nuclear Company, NRTS)	SS-SSBD	Same as for Semiscale-Plexiglass Vessel with steam-water countercurrent flow and in addition, effect of heat transfer from fluid boundaries at elevated temperatures.	12-ft long, 8.5 in.-ID pressure vessel, 1/2-or 1-in. annulus width, outlet phase operator.	Last quarter of 1972		Steady-state air-water and steam-water countercurrent flow tests to study effects of water temperature, wall stored energy, downcomer gap width, and lower plenum geometry.
3. 1-1/2-Loop Semiscale - Isothermal Tests (USAEC, Aerojet Nuclear Company, NRTS)	I-SSBD	Investigate ECC delivery with emphasis on the applicability of steady-state low pressure results to transient blowdown conditions.	Semiscale vessel; vessel downcomer; operating loop with pump, steam generator, and pressurizer; blowdown loop with simulated steam generator and pump; accumulators.	Mid 1973		Test conditions include blowdown with and without ECC injection; variation in ECC injection pressure and location, lower plenum geometry, pump shutdown times, and steam generator secondary conditions.

TABLE II (Contd.)

ONGOING AND PLANNED RESEARCH AND DEVELOPMENT RELATED TO LOCA-ECC IN WATER COOLED REACTORS

PROGRAM	CODE	PURPOSE	EXPERIMENTAL SYSTEM	SCHEDULE	REF.	REMARKS
4. Steam-Water Mixing Test Program (USAEC and Combustion Engineering, Inc. Windsor, Connecticut)	CE/USAEC	Investigate interaction of the ECC fluid with steam in the primary system.	Steam generator; cold leg with loop seal, simulated pump, and ECC injection nozzle; and reactor vessel. Test section is a geometrically scaled model of the piping from the steam generator to the reactor vessel inlet.	Continuing	39	Investigate piping hydraulic resistance, injection nozzle inclination, test section size. Equilibrium tests at various pressures to obtain data on water remaining, and influence of reactor vessel geometry on water remaining.
<u>Reflooding Heat Transfer</u>						
1. Low Rate ECC Reflooding Heat Transfer (FIAT Nuclear Energy Section, Italy)	FIAT	Obtain rod bundle heat transfer data during reflooding.	Twenty-one heater rod assembly with upper and lower plenum. Heater OD is 9.8 mm, length is 1184 mm, and pitch is 12.9 mm. Uniform power distribution.		40	Flooding rates in the range of 0.2 to 0.6 in./sec, power varied from 0.45 to 0.65 kW/ft. A relationship between bottom flooding and maximum acceptable power was obtained.
2. 1-1/2-Loop Mod-1 (USAEC, Aerojet Nuclear Company, NRTS)	Mod-1	Includes investigation of blowdown and ECC injection variables and their influence on cladding temperature.	A simulated PWR system with generic component elevation, electrically heated core, operating loop, blowdown loop, accumulator, HPIS and LPIS.[a] Capability for 5.5- and 12-foot heated rod lengths.			Emphasizes system and loop component effects on ECC delivery to core region during blowdown.
3. ECC Reflooding Experiments (Siemens, Germany)	Siemens/GFM	Investigate the performance of low pressure ECCS and obtain fluid flow and heat transfer data during reflooding.	Preliminary reflooding tests with a single internally cooled tube. Also an electrically heated 340-rod array, 10.75-mm rod diameter, 3 m in length with chopped cosine power distribution. Bundle has three regions which can be independently heated with maximum power input of 1.4 MW. A 16-bar pressure vessel contains the bundle. A bypass simulates the annular downcomer.	Continuing	41	Flooding from the bottom only and simultaneous top and bottom flooding.
4. Loss-of-Fluid Test (USAEC, Aerojet Nuclear Company, NRTS)	LOFT	Obtain data on thermal-hydraulic behavior associated with core and on core thermal response during ECC injection.	55 MW(t) PWR with an operating loop, a blowdown loop, accumulators, LPIS, HPIS[a], supporting systems, and initially a 5.5-ft-long core of typical PWR fuel dimensions with about 1300 fuel pins. Capability for a 12-ft-long core provided.	Continuing		Reflooding heat transfer data on nuclear fueled core. Wide range of ECC injection parameters and injection locations can be selected.

TABLE II (Contd.)

ONGOING AND PLANNED RESEARCH AND DEVELOPMENT RELATED TO LOCA-ECC IN WATER COOLED REACTORS

PROGRAM	CODE	PURPOSE	EXPERIMENTAL SYSTEM	SCHEDULE	REF.	REMARKS
5. Power Burst Facility (USAEC, Aerojet Nuclear Company, NRTS)	PBF	Determine maximum fuel cladding temperature permitted at the time of coolant delivery without loss of cladding integrity, the effect of ballooning on maximum permissible temperature, and the effect of degraded coolant performance on the maximum permissible cladding temperature.	Open tank reactor vessel housing 3-ft-long driver core, a central flux trap region containing an in-pile tube in which the test fuel is located and a loop coolant system for providing required system conditions in the test section.	Continuing		Tests with both PWR and BWR fuel clusters; up to 24-pin array. Top spray or bottom flooding capability. Tests include use of irradiated fuel.
<u>Scaled System Effects Experiments</u>						
1. 1-1/2-Loop Mod-1 (USAEC, Aerojet Nuclear Company, NRTS)	Mod-1	Provide LOCA-ECC data; for analytical model evaluation of total system codes, on effects of physical scale in relation to LOFT scale, and for guidance of LOFT test program.	A simulated PWR system with an electrically heated core, an operating loop, a blowdown loop, accumulator, HPIS, and LPIS[a]. Capability for 5.5- and 12-ft-long cores and other system geometry changes.	---		Extensive systems effects and parameter variations on system component influence on blowdown, blowdown heat transfer, ECC delivery, and core reflooding cooling.
2. BWR Blowdown Heat Transfer (USAEC and General Electric Company, San Jose, Calif.)	GE/BDHT/AEC	Provide information on the transient heat transfer following a rupture of a steam line or recirculation line in a BWR.	Scaled BWR system consisting of a pressure vessel with two external drive pump recirculation loops. Vessel contains a full-size 49-rod electrically heated bundle, two jet pumps, and a steam separator.	Continuing	42	Testing conditions to include: variation of initial system conditions, break size and locations, and power decay transient.
3. Research of Safety Assessment (ROSA), (Japan Atomic Energy Research Institute Tokai Laboratory)	ROSA II	Obtain information on the effects of system parameters such as piping resistance, power, pressure, break location and size, and ECCS variations, for both BWR and PWR conditions.	ROSA II facility consists of a 265-mm-ID and 5900-mm-long pressure vessel containing 109 electrically heated rods 1500 mm in length, two loops with pump and steam generators, one of these loops has a rupture unit, pressurizer, and ECCS.	Continuing	32	ROSA II facility is being fabricated. Testing is scheduled to begin in October 1973.
4. Full Scale Safety Experiments of FUGEN	FUGEN	Obtain cladding temperature information during blowdown and ECC injection, discharge reaction forces, and transient characteristics of the primary system.	The facility consists of a steam drum, circulating pump, downcomer, lower header, 25 pressure tube assemblies 3 of which contain electrically heated full-scale fuel assemblies, and ECCS. Fuel assemblies contain 28 heaters 3.7 m in length.	Continuing	43	Experimental facility is a mockup of the prototype, heavy-water-moderated, boiling-light-water-cooled reactor, FUGEN.

TABLE II (Contd.)

ONGOING AND PLANNED RESEARCH AND DEVELOPMENT RELATED TO LOCA-ECC IN WATER COOLED REACTORS

PROGRAM	CODE	PURPOSE	EXPERIMENTAL SYSTEM	SCHEDULE	REF.	REMARKS
5. Full Length Emergency Cooling Heat Transfer-System Effects Tests (USAEC and Westinghouse Electric Corporation, Pittsburgh, Pa.)	FLECHT-SET	Provide experimental data on the influence of system effects on ECC behavior during reflooding.	FLECHT flow housing and a power profiled 10 x 10 heater rod test assembly, coolant supply system, downcomer, pressure control system, upper and lower test section plenums, and single loop consisting of piping, valves, and orifices. In later tests the single loop will be replaced by two loops each having a full length steam generator, pump simulator, and piping arranged to represent elevations and flow characteristics in the loops.	End 1973	29	Tests emphasize the influence of system characteristics on flooding rate, heat release from cladding and flow housing, carryover, and pressure drop. Additional system features and testing are being considered.
<u>Large-System Effects Experiments with Nuclear Heat Source</u>						
1. Loss-of-Fluid Test (USAEC, Aerojet Nuclear Company, WRTS)	LOFT	Provide PWR integral test data on all principal aspects of an LOCA with ECC injection including transient thermal, mechanical, and nuclear response of the system, capability of ECCS, margins of safety, containment system effects, and fission product behavior.	55 MW(t) PWR with an operating loop, a blow-down loop, accumulators, LPIS, HPIS[a], supporting systems, and initially a 5.5-ft-long core otherwise of typical PWR fuel dimensions with about 1300 fuel pins. Capability for a 12-ft-long core provided.	Continuing		Test data will provide unique experimental information; at large scale, in more than one dimension within core and reactor vessel, with generic time constants of nuclear fuel, with interacting effects of cladding ballooning, changing fuel gap conditions and changing coolant channel geometry, and at high temperature fuel conditions for determining margins of emergency core cooling performance.

[a] HPIS -- high pressure injection system; LPIS -- low pressure injection system.

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Reprinted, by permission, from Brockett, G.F., R.W. Griebe, R.W. Shumway, and J.O. Zane, "Loss of Coolant: Control of Consequences by Emergency Core Cooling", Proceedings of the International Conference on Nuclear Solutions to World Energy Problems, Joint ANS/AIF Conference, November 13-17, 1972, Washington, D.C.

Reference 10) and the American Nuclear Society

Appendix 5 REACTOR FISSION PRODUCT DECAY HEAT

Fission product decay heat produces one of the primary energy inputs for the ECCS. Unlike many of the complicated coupled fluid mechanical thermodynamic-heat transfer problems associated with the overall effectiveness of the ECCS, the decay heat, as an energy source function for this system, may be evaluated independently of the remainder of the problem. Consequently, the analyses leading to a definitive specification of the magnitude of the decay heat might rationally be expected to have been adequately disposed of long before the preparation of the IAC. Regrettably, the magnitude of the decay heat was the subject of substantial controversy during the ECCS Hearings. The principal arguments associated with the subject will be reviewed in this appendix.

A5.1 Decay Heat Standards

The IAC specified that the decay heat input utilized for the analysis should be defined in accordance with the proposed American Nuclear Society (ANS) Standard ANS 5.1, "Decay Energy Release Rates Following Shutdown of Uranium-Fueled Thermal Reactor," with an added 20 percent "allowance for uncertainties."* During the early stages of the hearings, the CNI provoked a substantial controversy over the adequacy of the ANS Standard 5.1 on the basis of an investigation on the subject of decay heat by T. R. England. The CNI suggested, based on England's doctoral dissertation results, that the ANS Standard 5.1 might underpredict decay heat by 20 percent to 50 percent. Subsequent reviews and analyses of the decay heat have substantially reduced the controversy over the problem (as well as the expected magnitude of ANS Standard 5.1 underpredictions).

* See appendix 3: Interim Acceptance Criteria, Appendix A: Part 1, No. 5; Part 2, No. 6; Part 3, No. 2, etc.

The technical community's concern over the adequacy of the ANS Standard 5.1 + 20 percent prescription of the IAC has not been completely eliminated. However, supplemental studies imply that there are no major discrepancies in the ANS Standard 5.1 description of decay heat (at least of the magnitude initially suggested by England and the CNI). As a result, the IAC decay heat prescription was adopted in the AC, without change. Subsequent review and analyses of decay heat have substantially reduced (but not eliminated) the controversy over the problem.

In an operating reactor, in addition to fission product decay heat generation shown in figure A5.1, thermal energy is also supplied after shutdown by delayed neutron interactions and heavy isotope (U-239 and Np-239) decay. Figure A5.2 shows a typical total shutdown power generation curve, demonstrating the rapid decrease in the effectiveness of delayed neutrons as a power source. As shown in figure A5.2, delayed neutron interactions act to maintain heat generation at relatively high levels during the first 10 seconds (approximately) after shutdown. At the end of this period, the shutdown power has decayed to about 6 percent of rated power output for the reactor and is subsequently a function of essentially fission product and heavy isotope decay only. At the end of 100 seconds, the power has decreased to less than 4 percent of rated power and continues to decrease thereafter at a rate approximating the 0.2 power of time.

The ANS Standard 5.1 has been based upon a combination of numerical and experimental studies. One of the principal problems with the analysis upon which the standard is based is the shortage of experimental results for both the critical 0-1000 sec time period following shutdown and for fuel exposed in reactors for extended-high flux irradiation periods (10,000-50,000 hours) (4, p. 22-9). Experimental information on decay heating is gathered in two ways: through results of gamma or beta energy decay measurements, or by direct calorimetric measurements.

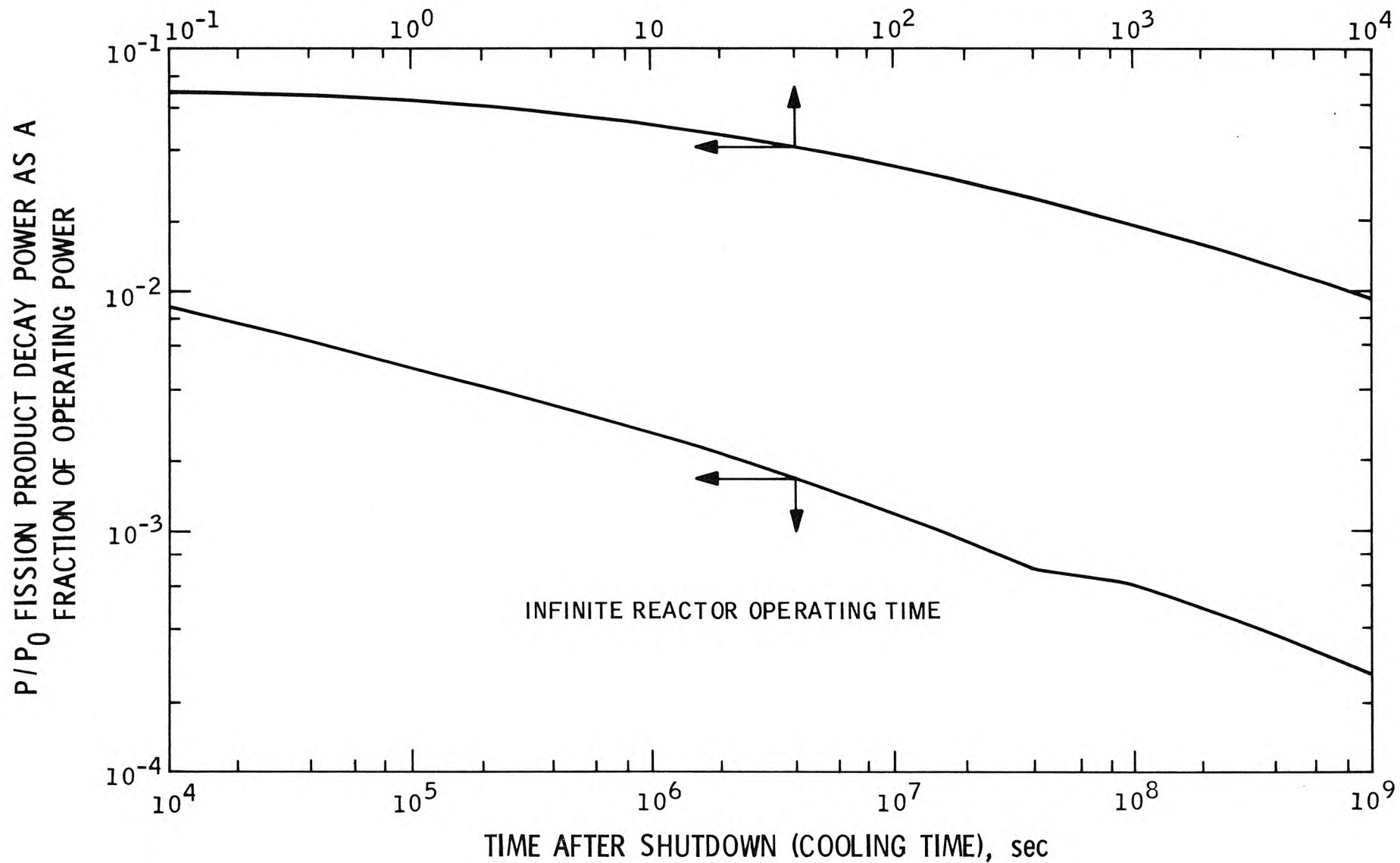
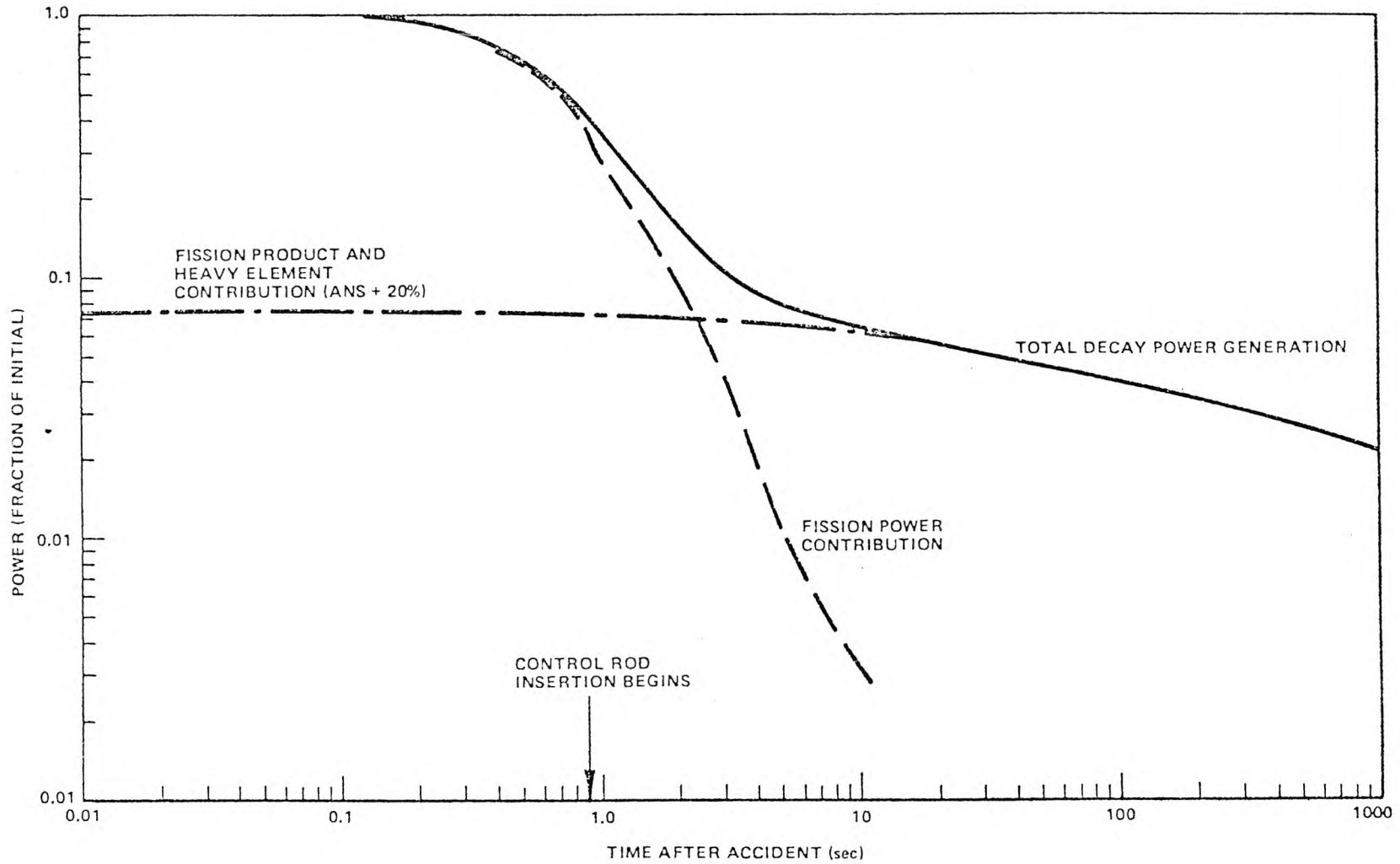


Figure A5.1 ANS Standard 5.1 Fission - Product Decay Heat Curve
(For Uranium-Fueled Thermal Reactors)

Figure A5.2
Normalized Reactor Shutdown Power Generation



(After Figure 6-1, 66, by permission.)

No direct calorimetric methods have been successfully employed for shutdown times less than 1000 sec; calorimetric results exist for experiments conducted on fuel rod elements exposed to irradiation bursts of short time periods which agree "satisfactorily" with numerical studies. All of the important analyses of decay heat depend upon experimental results of gamma and beta energy measurements during the important 0-1000 sec time period following shutdown. Unfortunately, there are only about five to six good independent sets of experimental data for each of the gamma and beta measurements -- a very small quantity of data for such important parameters.

ANS Standard 5.1 also depends on experimental results in the time period 0-1000 sec and results of numerical studies for the modeling of subsequent time periods. Until recently, numerical analyses were considered to be of questionable validity in the 0-1000 sec shutdown period because the influence of short half-life fission product nuclides was generally neglected in the summation calculations used for the analyses.

A5.2 Comparison of IAC and T. R. England Decay Heat Predictions

An important numerical analysis of reactor shutdown heating was recently performed by T. R. England (23). The England study was a numerical summation calculation in which an attempt was made to improve upon other available short time studies by using more recent data sources and theory, including evaluation of short-lived isotopes in the analysis, and the improvement of the neutron capture analysis with a more complete physical modeling of the coupling of the nuclides of the fission product chain. These characteristics tended to improve the short time analysis. Consequently, England's study claimed to be valid for time as short as 60 sec or more after shutdown.

Prior to England's calculation, uncertainty over the physical characteristics of the short-lived isotopes (i.e., yields, half-lives, capture cross-sections, and beta and gamma decay energies for isotopes

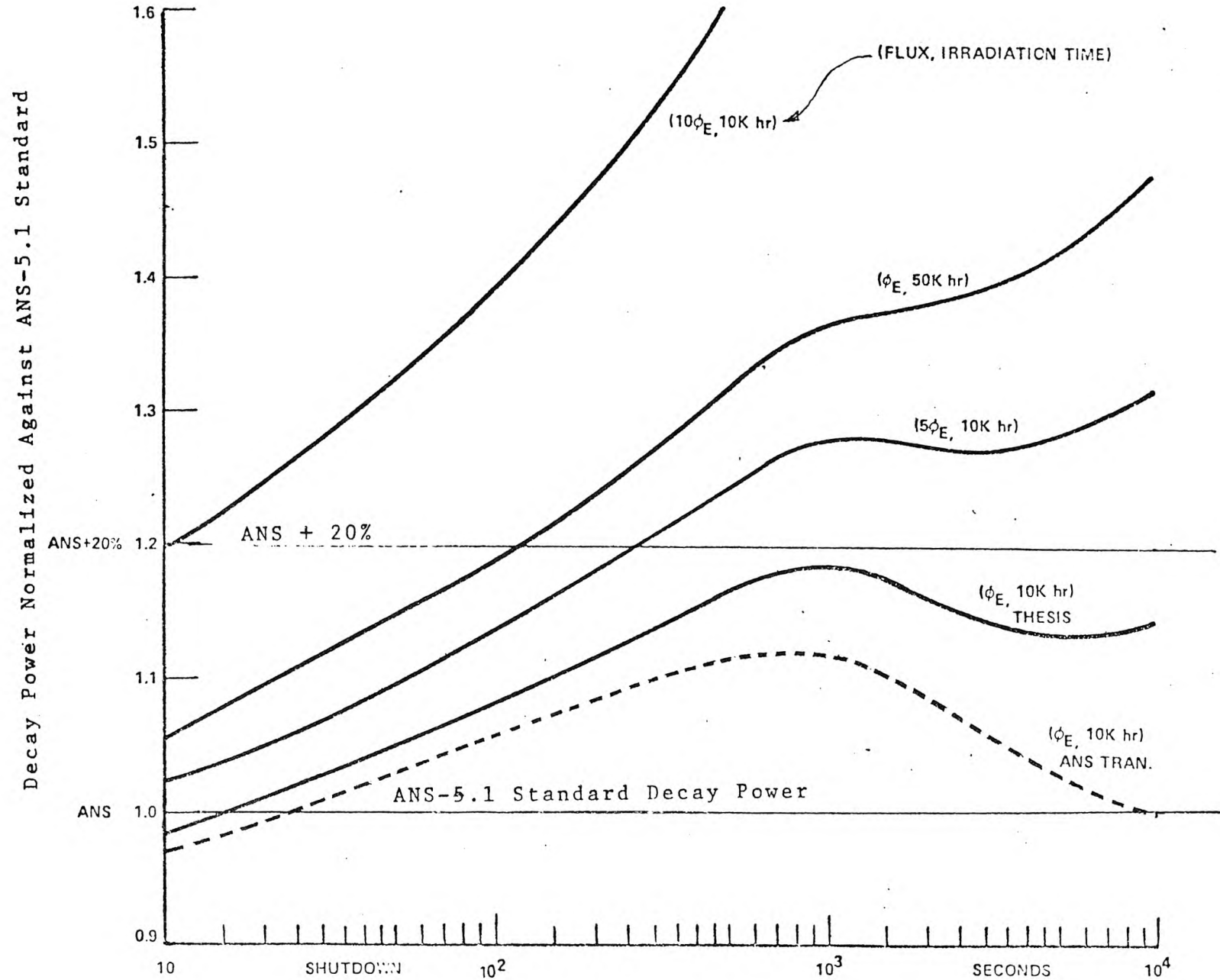
with half-lives less than one minute) limited estimates of summation calculation validity to cooling periods greater than 1000 seconds. The earlier analysis (Shure, 1961, 63) upon which ANS Standard 5.1 was based used experimental data to provide the basis for decay times less than 1000 sec. Shure's early study was based (for decay times greater than 1000 sec) upon summation calculations which included 350 fission product nuclides and employed neutron absorption in addition to radioactive decay to account for coupling between nuclide decay chains (65, p. 5). Neutron coupling was not included in the studies upon which Shure's early work was based. Future studies which are to be conducted under AEC sponsorship may include as many as 800 nuclides in describing decay energy. Adequate descriptions of decay times as short as 10 sec may be possible in summation calculations using the current nuclide data.

The most notable departure of England's results from the ANS Standard 5.1 was associated with an indicated increase in the influence of time-dependent neutron flux-irradiation time characteristics on the decay heat. England's results, for sufficiently large flux-time intervals, were considerably larger than predicted by the ANS Standard 5.1 model with an infinite irradiation period. To graphically demonstrate the differences, England's original results for the time-history of decay power are shown (normalized against the ANS standards) in figure A5.3. The results are plotted in terms of the parameters of multiples of a standardized flux, ϕ_E , and irradiation times. It should be noted that these results were based upon calculations which were erroneous. Corrections were made in the code and the results were revised as indicated by the dashed curve in figure A5.3.

England's base case, with the standardized flux ϕ_E , corresponds to a thermal neutron flux of 2×10^{13} for 10,000 hours. This case was calculated with a U-235 density of 5×10^{-4} at/barn-cm² (England's units) which produces an initial power density of 109 w/cm³, for the unit flux ϕ_E .

Figure A5.3

Comparison of England Fission-Product Decay Calculations to ANS Standard 5.1



Typical BWR and PWR reactors have average U-235 enrichments of 2.7 and 2.15 percent, respectively, corresponding to U-235 densities of 1.8 and 1.2×10^{-4} at/barn-cm². With power densities for typical \bar{W} -PWRs and GE-BWRs of the order of 105 w/cm³ and 51 w/cm³, respectively, the above U-235 densities would lead to average flux levels for the PWR of $2.7\phi_E$ and $1.9\phi_E$ for the BWR, in England's nomenclature. For these reactors, an average equilibrium burnup of 30,000 MWd/MT* would correspond to three annual 8000 hour cycles for the PWR and five for the BWR (4, p. 22-10).

In accordance with the IAC, ECCS analyses are based upon evaluation of a fuel rod exposed not to the average flux, but to the influence of the peaking factor resulting in the "most severe thermal transient" possible for the accident. To satisfy these requirements, a peaking factor of 2.6 is appropriate for DBA analysis. Under these circumstances, a flux-time integral of ($7\phi_E$, 24Khr) for the PWR and ($5\phi_E$, 40Khr) for the BWR would be representative of limiting conditions (a single rod exposed to maximum flux conditions over its entire life span in the reactor) of irradiation exposure for the reactor fuel.

England's original dissertation results, shown in figure A5.3, imply that flux-time integrals of the above magnitudes would lead to shutdown decay energies substantially in excess of those predicted by the ANS Standard 5.1. The results (solid lines) indicate the initially predicted (on the basis of erroneous calculations) effect of increasing irradiation time and flux on resultant decay power. The results implied that increasing irradiation time by a given factor, generally speaking, might produce a greater increase in decay power than the same relative increase in flux. The nonlinearity of the results shown (as well as their apparent magnitude) in which increasing fluxes and irradiation times led to apparently exponentially increasing decay power led the CNI to become very concerned over this problem. England's conclusions to his dissertation had stated:

* MWd/MT: megawatt days per metric ton of uranium.

Because previously recommended heating rates, such as Shure's, have relied on a calculational model which ignores neutron absorption, several generations of coupled progeny and coupling systematics in general, this would explain the differences observed here. Prudence in design would require an increase of 20-50% in estimated heating rates now in use (23).

A5.3 Shure/England Reevaluation of Decay Heat

The substantial apparent differences between England's dissertation results and the ANS proposed standards caused substantial concern within the technical community over the validity of the results and the sources of the apparent conflicts. In order to investigate the causes of the apparent decay heat differences, Shure initiated a reevaluation of his original recommendations together with England's calculational methods. Though the study was apparently conducted primarily by Shure, he received the cooperation of England in analyzing, correcting, and rerunning his code (CINDER) which had been developed and applied in England's doctoral dissertation.

The CINDER code, which includes "about 350 fission product nuclides and employs neutron absorption in addition to radioactive decay for coupling purposes between nuclides," was acknowledged by Shure to be a "more sophisticated" method than had been employed in his original investigation (64, p. 5). However, in the course of a careful investigation of the code, in comparison with other similar calculational routines, it was discovered that a "very subtle programming bug" had been made in one of the critical steps of the CINDER code (64, p. 7). After this programming error was corrected by England, further investigations turned up "several data errors" which were subsequently revised in CINDER's tabulated data for the physical parameters of some of the critical nuclides (64, p. 7). It should be observed that a similar number of "corrections" were required in the library of nuclide physical parameters for Shure's own current code (FSTAB) (64, p. 7).

With CINDER's programming errors debugged and the corrections to the nuclide physical parameters incorporated, results of the code were again compared with the proposed ANS Standard 5.1 decay heat standards. Comparative data are shown in table A5.1, based upon Shure's reported results (64). It can be seen that while England's dissertation results departed from the proposed standard by 10-20 percent in the critical period of about the first 1000 sec of LOCA shutdown (figure A5.3), with the revisions and corrections the deviation was less than 6 percent for the base (or "fiducial") case ($1\phi_E$, 10K hr).

To evaluate the importance of neutron absorption and coupling in the nuclide chains, Shure investigated higher flux variations for the 10K hr fiducial case (flux levels of $5\phi_E$ and $10\phi_E$). Results are shown in table A5.3, in comparison with the revised CINDER results of table A5.1 and England's dissertation results. With the corrections and adjustments to CINDER made, the higher flux results ($5\phi_E$ and $10\phi_E$) can be seen to be much more in line with ANS Standard 5.1 than the original dissertation results (figure A5.3 and column 4). With corrections, increases can be observed of 1-3 percent above the fiducial CINDER case, rather than the factors of nearly 2 which were observed in the original dissertation.

Review of the results indicated that the principal cause of the apparent reduction in relative energies was associated with the fission rate history of England's original dissertation. Comparison of the results indicated that the constant flux assumptions of England's dissertation led to unrealistically low fission rates (number of fissions/sec) near the end of the irradiation period (~10K hr) as a result of uranium depletion. Low fission rates imply relatively low power output for the fuel. In practice, the average fission rate for the reactor is held nearly constant -- for a case of constant reactor power output -- requiring a relatively increasing flux density rather than a constant flux history. As a result of England's constant flux assumption, the apparent relative power output near the end of the problem from the fuel (in Mev/fission) was overpredicted, due to extremely low relative fission rates

Table A5.1
Comparison of ANS 5.1 Standard Decay Energy with England Dissertation
and Revised CINDER Results

("Fiducial" Case-- $1 \phi_E$, 10,000 Hours Irradiation)

t (sec)	ANS Standard 5.1 (Mev/fiss)	England Dissertation CINDER (Mev/fiss)	CINDER ANS Standard 5.1	Revised CINDER (Mev/fiss)	Revised CINDER ANS Standard 5.1
0	13.88	11.68	.841	11.51	.829
10^1	9.874	9.825	.995	9.317	.943
10^2	6.632	7.160	1.080	6.639	1.001
10^3	3.729	4.381	1.175	3.943	1.057
10^4	1.836	2.205	1.201	1.853	1.009
10^5	0.8428	1.130	1.341	0.8724	1.035
10^6	0.4197	0.5453	1.299	0.4369	1.041
10^7	0.1171	0.1571	1.342	0.1163	.993
10^8	0.006620	0.007877	1.190	0.007621	1.151

Table A5.2

Effect of Neutron Absorption of U^{235} Fission Product Decay
Energy for the 10,000 Hour Fiducial Case

(Interim Revised Physical Parameters Used as Well as Revised CINDER Program)

t (sec)	(1) Reduced ϕ Rev. CINDER	(2) 5 ϕ Rev. CINDER	(3) 10 ϕ Rev. CINDER	(4) 10 ϕ^* Rev. CINDER
0	1.00	1.01	1.01	1.18
10^1	--	--	1.01	1.21
10^2	--	--	1.02	1.27
10^3	1.00	1.01	1.02	1.43
10^4	1.00	1.01	1.03	1.84
10^5	0.98	1.02	1.05	2.61
10^6	0.98	1.02	1.05	3.60
10^7	0.98	1.05	1.10	5.42
10^8	0.92	1.30	1.60	10.01

* Fission rate history is comparable to case 12 ($10 \phi_E$, 10K hr - England's dissertation). All other fission rate histories are comparable to the fiducial case (case 1) of ref. 23.

(Mev/fission = Mev/sec \div fission/sec). The results shown in table A5.2 reflect calculations in which fission rates have been arbitrarily maintained at the same relative rates as the fiducial case, for all flux variations considered.

A5.4 ORNL Review of Empirical Decay Heat Results

Under AEC support, Perry, Maienshein, and Vondy of ORNL investigated the empirical data supporting the proposed ANS Standard 5.1 standard (65). The results of the study are summarized in figure A5.4 reproduced from Perry, et al. (65). In the figure, results of various analyses of fission-product decay heat have been compared, on a normalized basis, with the ANS Standard 5.1 (curve (1)). Curve (2) represents the "best estimate" of the experimentally derived results of Perry et al. for fuel which has been irradiated in the reactor for an "infinite" time. It can be seen that in the critical period from 100 to 10,000 sec, the results depart from the ANS Standards 5.1 by a maximum of about 6 percent (occurring at 1000 sec). Other results shown in the figure include results of a recent Shure (FSTAB) calculation (curve (3)) of infinitely irradiated fuel (64). The FSTAB results demonstrate the characteristic inability of summation calculations to adequately predict short time results (especially for those calculations which include a relatively small number of nuclides - 201 - in their analysis). As a result, Shure's analyses (e.g., 63) customarily described the first 1000 seconds after shutdown in terms of experimental results (curve (5)).

Curve (4) presents a graphical comparison of CINDER results (similar to those shown in table A5.1) for the so-called fiducial case ($1\phi_E$, 10K hr irradiation) of Shure and England's studies. Shure's FSTAB results for a similar calculation have been shown in curve (5). The most significant observation to be drawn from the comparison is that all of the studies presented show a consistent departure, on the order of 5-8 percent, in the period of about 1000 seconds (and later) after LOCA shutdown.

The 6 percent deviation predicted by England at 1000 seconds after shutdown appears to be wholly consistent with, and thoroughly supported by, all the latest results.

Perry, et al. conducted an independent numerical investigation of the effects of neutron coupling using the ORNL-ORIGEN code in a summation calculation. England has attributed the dominant role in the increases in decay energy predicted by his dissertation study to neutron coupling in the fission-product nuclide chains. Using the ORIGEN code, Perry observed an increase in decay heat attributable to neutron coupling of no more than 2 percent for cooling times less than 1000 sec, a 4 percent increase at 10^4 sec, and a 7 percent increase at 10^5 sec (65, p. 53) for fuel irradiated at average power levels within the reactor (approximately $2-3\phi_E$ in England's nomenclature) for 30,000 MWD/MT. Though this does not correspond to infinite irradiation time periods, it does represent approximately 50,000 hours of irradiation at the average power levels of the calculation. Exposure times of this magnitude for fuel should furnish an adequate check on the validity of England's assertion of important long irradiation time effects to be expected due to neutron coupling. Though the results do indicate a potential 2-4 percent increase, Perry's data do not support the kind of results for long irradiation periods suggested by England.

As further evidence of the validity of Perry's results, he conducted an investigation of coupling effects for a case approximately the fiducial exposure period of 10,000 hours (at the same flux levels of $2-3\phi_E$). Perry's results for this case agree well with the results of Shure (table A5.2). Increases in decay energy for this case were less than 2 percent for all shutdown times less than 10^5 sec in Perry's "fiducial" study (65, p. 53).

Thus it appears that the combined effects of high fluxes associated with the hottest rods of the reactor ($5-7\phi_E$) and long irradiation

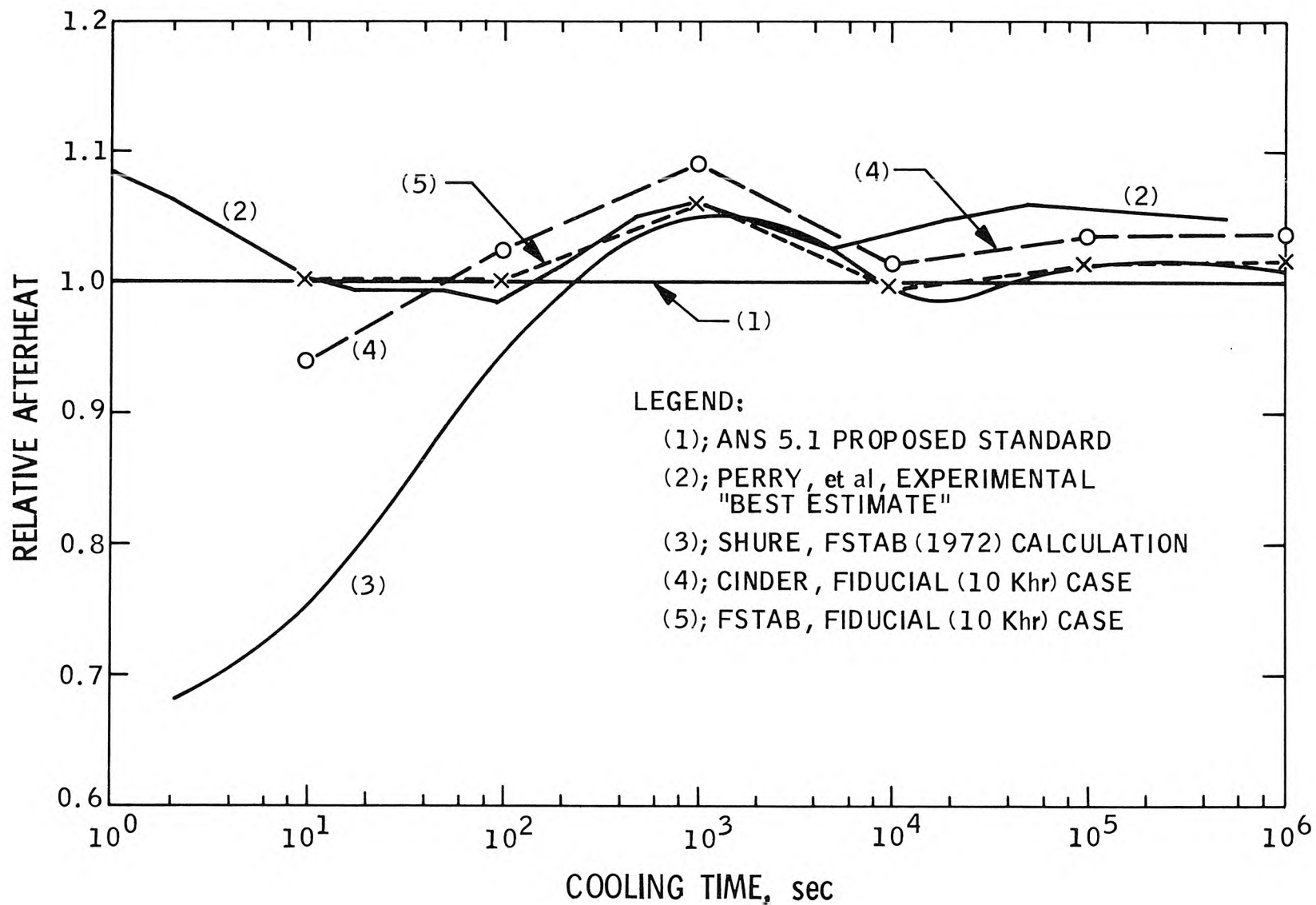


Figure A5.4 Relative Integral Afterheat, $F(t, T)$, from Various Sources (Relative to ANS Standard 5.1)
(After Figure 22, 34, by permission.)

times (up to 50,000 hours) should not increase decay power more than 4-7 percent above the best estimates of decay energy for "fiducial" flux levels and irradiation times in the critical period of about 1000 sec after LOCA shutdown. However, this evidently is in addition to an expected 5-8 percent positive deviation from the ANS Standard 5.1 in this same time period for the fiducial case. Thus the total expected deviation from the ANS Standard 5.1 at about 1000 sec after shutdown for the hot rod conditions could well be an increase of from 10-15 percent.

A5.4.1 Experimental uncertainty in decay heat estimates

These estimates of deviations from the fiducial case must be coupled with consideration of the experimental uncertainty associated with the empirical data of Perry, et al. With respect to their "best estimate" values of figure A5.4, they have stated:

...we believe that there remains in the composite afterheat function, $F(t, \infty)$, and error (roughly in the sense of one standard deviation) of about 10-15 percent (65, p. 19).

The composite afterheat function, $F(t, \infty)$, is simply the resultant decay heat associated with both beta and gamma ray decay energies of the fission product nuclides. After a statistical evaluation of the data from which they derived their best estimate fit to the experimental data, Perry, et al. concluded:

The assertion that the standard error in the afterheat function $F(t, \infty)$ is as small as +7% seems a bit surprising, when the results of individual experiments are as discrepant as they are shown to be in Figs. 11 and 14. In Fig. 11, for example, it may be seen that over much of the time range covered all the [β decay energy] measurements lie outside of barely within the error band (+7-8%) deduced for $f_{\beta}(t)$. The pattern for gammas is somewhat similar though less clear cut. It is our feeling, on the basis of many years of experience with nuclear physics measurements, including

those of the type being evaluated, that the uncertainty in $F(t, \infty)$ would more conservatively be placed at +15% (65, p. 44) (emphasis added).

To further define the limits of uncertainty for the data, the upper bounds of the empirical data were developed. Figure A5.5 shows the upper bounds for the partial composite after heat function, $F^+(t, \infty)$, normalized against Shure's functional expression for the decay energy used in ANS Standard 5.1. For the limited experimental data examined by Perry et al., the results are bounded at the 1000 sec shutdown time by a + 28 percent increment. Over most of the remaining period of interest, the bounding value averages approximately 25 percent above the ANS Standard 5.1 values.

It must be emphasized that the bounding limits for the data are based upon a relatively sparse collection of independent experimental results. The composite afterheat function was formed on the basis of four independent experiments for β -energy release rates and five experiments for γ -rays. Though more experiments were included for the γ -ray energy spectral measurements, it was the conclusion of Perry et al. that:

The situation with respect to gamma energy-release measurements is somewhat less satisfactory than for betas (65, p. 28).

It seems unlikely that the bounding values would be as low as 25-30 percent above the ANS Standard 5.1 if a more exhaustive set of experimental data had been included.

A5.4.2 ANS estimates of ANS Standard 5.1 uncertainty

The ANS-5.1 Standards Committee also evaluated the uncertainty in the estimate of decay energy which they were proposing. Without expressly stating how their uncertainty results were related to a statistical standard deviation, the ANS-5.1 subcommittee expressed their estimate of the uncertainty of the results about the recommended curve to be of the order of +20-40 percent for the first 1000 sec after shutdown. For the shutdown period between 10^3 and 10^7 sec, they stated that an

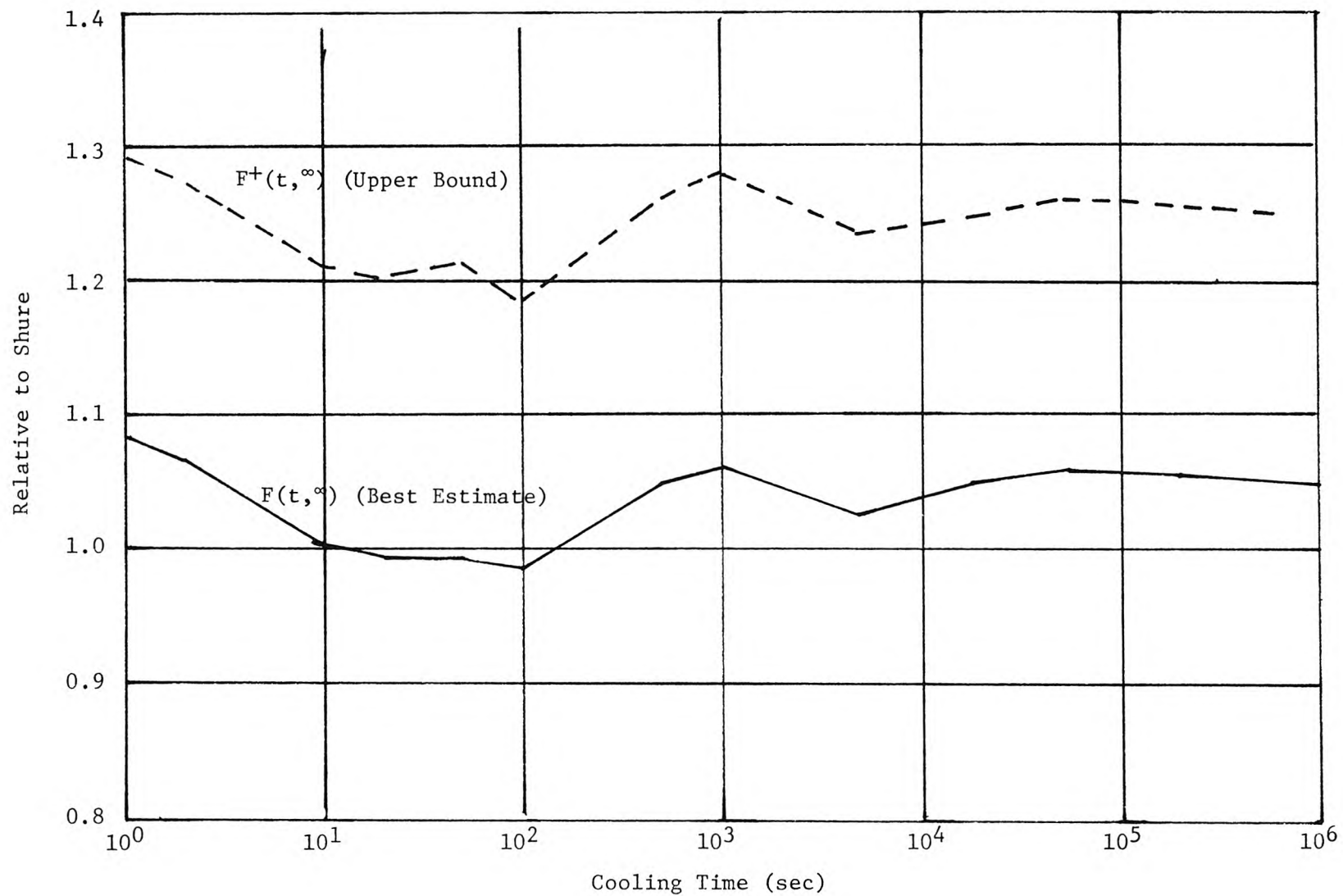


Figure A5.5 "Upper bound" for integral afterheat, relative to Shure (1961)
(From 65)

uncertainty of +10-20 percent existed. Beyond 10^7 sec (approximately one year, a period of relatively lesser concern for LOCA analyses) an uncertainty of +25 percent to -50 percent was noted. Thus the ANS Standard 5.1 +20 percent prescription of the IAC (and AC) does not represent a clear factor of safety, but apparently only covers one standard deviation of the recognized uncertainty in the evaluation of the proposed standards validity.

A5.5 Evaluation of Decay Heat Controversy

The Consolidated National Intervenor (CNI) group challenged the AEC's use of the ANS Standard 5.1 decay heat standard +20 percent as a conservative estimate of LOCA decay heat on two bases (5). They challenged it first on the philosophical basis that using the upper limit of the ANS standards uncertainty factor of +20-40 percent does not satisfy the need for application of a "safety factor" to the decay heat input conditions for the ECCS heat removal analysis problem. Second, they challenged the validity and conservatism of the ANS Standard 5.1 itself, claiming that England's work (23) made its adequacy uncertain.

A5.5.1 The semantics of "safety factors"

As far as the philosophical question relating uncertainty allowances and safety factors is concerned, the AEC has acknowledged that utilizing ANS Standard 5.1 +20 percent for the decay heat specification of the AC does not allow a "factor of safety" for this input factor. The Commission has stated:

...it appears to us that the 20% on top of the ANS decay heat formula fairly represents the uncertainty and does not provide any margin above that uncertainty (60, p. 1103) (emphasis added).

It may be argued that the decay heat is a parameter of such importance to the ECCS that a safety factor is a necessary requirement. However, it is not without precedent for safety factors of nearly unitary value to be applied in current engineering design practice. For complex

systems such as large aircraft or major military systems, it is considered both good and common engineering practice to use safety factors in a very restricted manner. The arbitrary application of even a relatively small safety factor to each design parameter in these complex systems could result in an overwhelming pyramiding of the factors as they were individually applied. Where careful attention to quality control is utilized and adequate research conducted to resolve uncertainties in the magnitudes of design parameters, it is accepted engineering practice to use bounding factors for statistically significant uncertainty limits for the parameters themselves, as opposed to formally specified safety factors which attempt to account for ignorance of such limits. This seems to have been the intent of the AEC treatment of the decay heat parameter.

A5.5.2 Decay heat prediction conservatism

The second question concerning the validity of the ANS standard itself and the adequacy of its uncertainty limits is a more significant problem. The CNI (5) used England's thesis result to support their position that the ANS Standard 5.1 decay heat estimates were too low. From our previous analyses, it is clear that England's initial dissertation results (fig. A5.3) have been shown to be invalid. The revised CINDER results of table A5.1 are representative of more accurate output from England's analysis when coding and input data errors have been corrected.

The revised results indicate a positive deviation of no more than 6 percent from the proposed ANS Standard 5.1, for the fiducial case (ϕ_E , 10K hr). When required corrections are made to the fiducial case to account for the higher neutron fluxes and longer irradiation times specified for "hot rod" calculations under the AC, deviations must be revised upward by an additional 4-7 percent. (Under AC specifications, expected neutron flux levels might correspond to $5-7\phi_E$, while irradiation periods would range from about 20K hr to 40K hr for total fuel irradiation levels of the order of 30,000 MWD/MT.) Thus the results of current sophisticated

summation calculations indicate the need for upward corrections to the ANS Standards 5.1 of the order of 10-15 percent in the critical 1000 sec cooldown period when operationally consistent high neutron fluxes and long irradiation periods are considered.

However, it is generally acknowledged that summation calculations have tended to be unreliable, particularly for short shutdown times of the order of 1000 sec or less (e.g., 65, p. 47; 64, p. 10). The unreliability is caused by inadequacies in information concerning yields, half-lives, and decay energies for the short-lived fission product nuclides far from the line of nuclear stability. Though there is reason to believe that substantial progress will be made in the near future, as a result of AEC sponsored research programs at ORNL, Battelle NW, and other locations, the reliability of summation calculations will continue to be uncertain in the immediate future.

Consequently, we must look to empirically based results for support to the quantitative results of the summation calculation. In this case, we observe (as shown in figure A5.4) that there is good general agreement between the results of CINDER calculations and empirical results for cooldown times greater than 100 seconds. The "best estimates" of Perry et al. give direct quantitative support to an upward deviation from the ANS Standard 5.1 of 6 percent or better at shutdown times of the order of 1000 seconds. Moreover, Perry, et al. have estimated a one-standard deviation uncertainty of the order of +15 percent in the empirical results.

Therefore, it appears that in the critical LOCA shutdown period of about 1000 sec, that ANS Standard 5.1 +20 percent will correspond to an equivalent deviation uncertainty of about one-standard deviation from the expected mean decay heat values. It is legitimate to ask whether confidence limits are adequately and conservatively bounded at one standard deviation. Since ANS Standard 5.1 +20 percent is equivalent to a one standard deviation uncertainty, the probability that higher decay energies

will be experienced above the AC prescribed limits is above 30 percent. Increasing the decay energy limits to ANS Standard 5.1 +30 percent would reduce the probability of experiencing decay heats above the criteria limits to about 11 percent. On the other hand, if the criteria limits were raised to ANS Standard 5.1 +35 percent, the probability is less than 5 percent that the criteria limits would be exceeded. A 30 percent probability of exceeding the AC (ANS Standard 5.1 +20 percent) limits seems excessive. Consequently, it would seem desirable to increase the bounding criteria values to either ANS Standard 5.1 +30 percent or ANS Standard 5.1 +35 percent.

In their opinion to the AC, the Commissioners have defended their selection of ANS Standard 5.1 +20 percent as follows:

Although no new experimental work was presented during the hearings, new computer calculations from the doctoral thesis of T. R. England were brought up and emphasized by the Consolidated National Intervenors (Exhibit 1152, pp. 2.2-2.6). England's work was essentially a computer calculation and summation of the contributions of individual nuclides, including for the first time the effect of neutron capture in the fission product chains. As originally presented, England's results indicated large deviations above the ANS prescription, particularly for high neutron fluxes and fuel burn-ups. (See, for example, Exhibit 1113, p. 22-5). However a series of errors in both input data and the calculational program were found both by England (Exh. 1178, p. 7) and in the course of a review by Shure (Exhibit 1178), which markedly reduced the deviations found by England's approach. With the corrections made, the positive deviations found by the England approach from the ANS standard are nowhere greater than 10%, and are generally much less (1113, at 22-15). In addition, there is the possibility that the selection of input data (fission product yields and decay energies) may not have been the best (1113, 22-8 and 22-9).

While England's approach is a valuable contribution, it is only one piece of work out of many cited in the record;

furthermore it presents no new experimental determinations. On the basis of the record of these proceedings, however, one is led to believe that the ANS standard curve may be about 5% low in the time region of principal interest, namely, zero to five minutes after shutdown. England's revised values are well within the previously expressed limits of uncertainty, and to the extent of the credence given the new calculations, they tend to narrow those limits of uncertainty. At present it appears to us that the 20% on top of the ANS decay heat formula fairly represents the uncertainty and does not provide any margin above that uncertainty. It is still conservative.

There is some margin provided, however, in the prescription requiring that the reactor shall have been considered to have operated continuously at 1.02 times rated power, with the maximum allowed peaking factor, for an infinite length of time. The exact amount of margin is uncertain, and it will vary with time, but it is probably in the range of 5 to 15% (Exhibit 1137, pp. 11-3 to 5; and Staff Concluding Statement, p. 114).

Considering all of the above, the Commission believes that the prescription of ANS + 20% for the fission product decay heat is reasonable and should be continued (60, pp. 1102, 1103) (emphasis added).

As discussed above, the arguments suggesting a 5-15 percent margin existing within the ANS Standard 5.1 +20 percent criterion appear to be tenuous. The deviations indicated by England's revised and updated CINDER code analyses seem to be well supported by the empirical data within a period of about 1000 sec after shutdown (a somewhat longer period than that favored by the AEC). The empirical data also suggest that ANS Standard 5.1 +20 percent represents only one-standard deviation from the mean decay heat results in this shutdown period. It is the opinion of the author that the probability of experiencing shutdown decay heats greater than the criteria specifications should be reduced to no more than 5-10 percent. On this basis, the criteria limits should be increased to ANS Standard 5.1 +(30 to 35) percent.

Appendix 6 INITIAL STORED FUEL ENERGY AND FUEL ROD GAS GAP
CONDUCTANCE

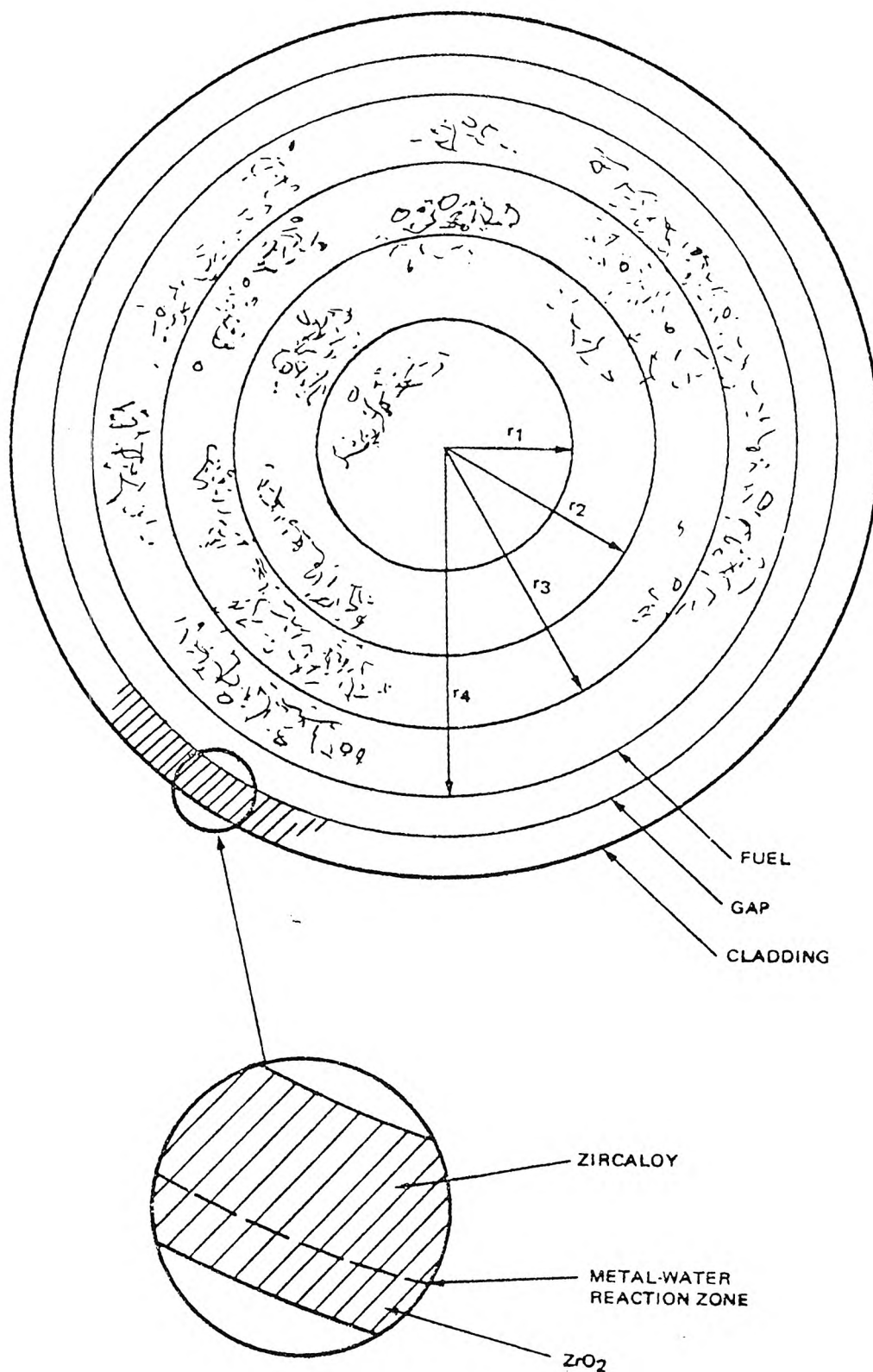
Obviously any uncertainties which might exist over the magnitude of the initial stored energy in the fuel would raise problems with predictions of ECCS response in the event of a LOCA. The problem is more complicated when uncertainties in the release rates of the stored energy are considered.

A6.1 The Physical Parameters of Initial Stored Fuel Energy

A typical idealized picture of the physical relations of elements influencing heat exchanged between fuel, fuel rod cladding and reactor coolant/working fluid is shown in figure A6.1. The stored energy is defined as the energy that would be released by the fuel if its temperature were reduced to that of the zircaloy cladding. Thus, the initial stored energy and its subsequent redistribution to cladding and coolant is a function of the parameters of fuel specific heat, its thermal conductivity, the thermal conductance of the gas gap and the heat transfer characteristics of the cladding and coolant.

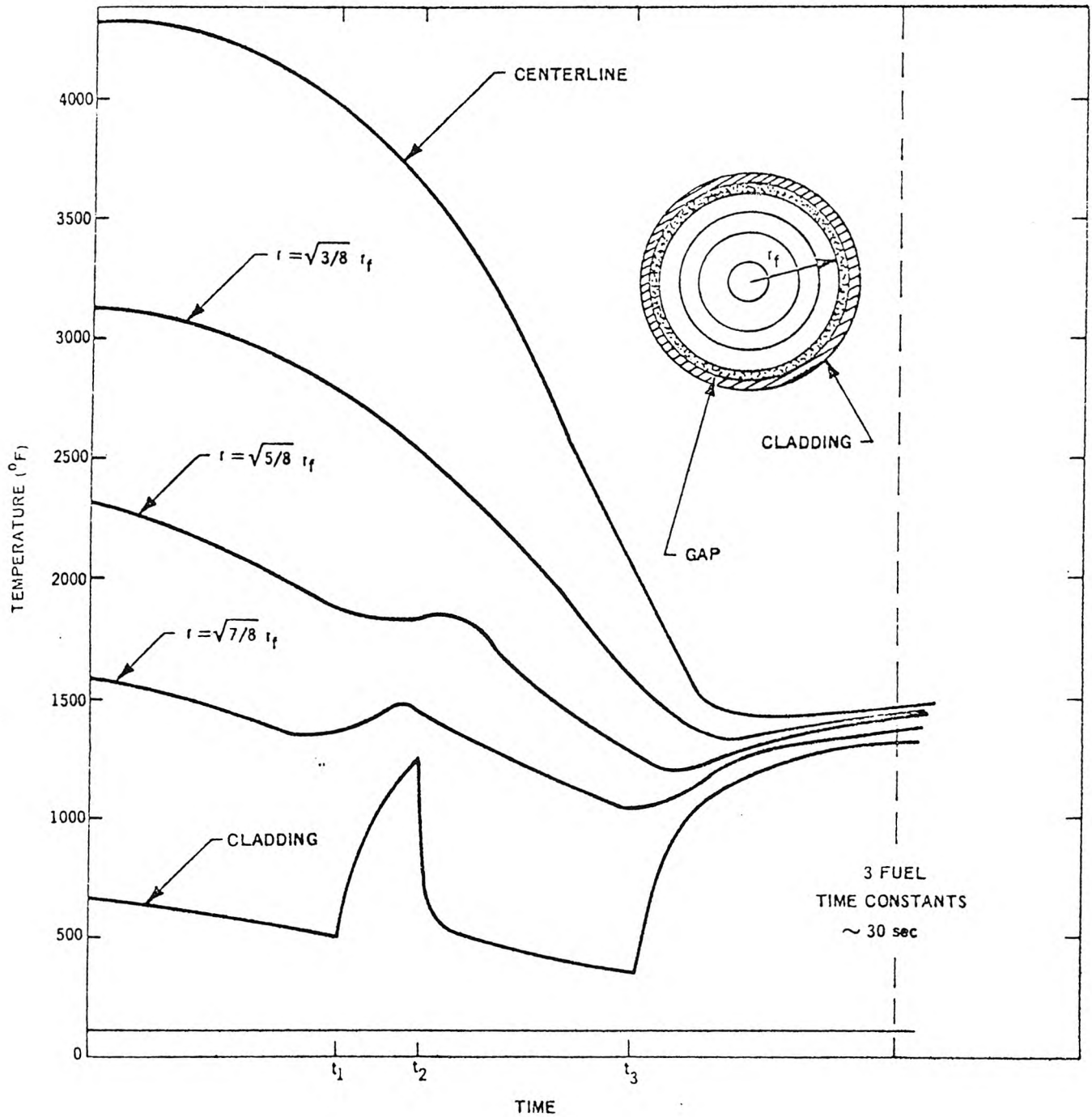
Following an accidental reactor shutdown, the stored sensible heat, associated with the heat capacity of the fuel and its high normal operating temperatures, represents a large heat source which must be dissipated by the ECCS -- in addition to the dissipation of the energy associated with the decay heat and other external heat sources such as exothermal metal-water reactions. A typical, schematic, representation of the temperature distribution within a fuel rod as a function of time is shown in figure A6.2. Initially the radial temperature gradient in the fuel rod is very steep. Centerline temperatures for the fuel approach the melting point (in excess of 4000°F) as a result

Figure A6.1
Idealized Fuel-Cladding Representation



(After Figure D-2, 53, by permission.)

Schematic Temperature Distribution



(After Figure 2-11, 53.)

of the high power output associated with normal operations, while clad temperatures are approximately 600°F. As soon as shutdown occurs, the temperature redistributes rapidly within the fuel, as shown in figure A6.2. The rod temperature history and the amount of heat transferred to the decompressing coolant depend strongly upon the thermal properties of the fuel and the physical configurations of fuel, cladding and associated gas gap (the gap geometry resulting from the previous history of fuel fabrication, irradiation exposure history, and consequent fuel densification during normal operation, and possible swelling and rupture of cladding during the thermal excursion of the accident) as well as the heat transfer conditions at the cladding surface induced by the flow of coolant during the accident. Although the importance of the stored energy was recognized at the time the IAC were issued, no general rule was formulated or specified for its evaluation (60, p. 1101). Uncertainty in the conservatism of the method of specification of these parameters, especially fuel and gas gap thermal properties, apparently caused the ACRS to include the initial stored energy of the fuel in their list of items of uncertain conservations given in response to the CNI questions (chapter 2). The new AC have attempted to rectify this oversight and make this an area of assured conservatism. In these efforts, the Commission appears to have been largely successful in achieving the degree of conservatism needed.

After initiation of a major LOCA, within approximately 30 seconds the temperature distribution across the fuel and cladding is nearly flat, as indicated in figure A6.2. This is a result of the relatively low power generation associated with fission product decay heat and the relatively poor heat transfer characteristics associated with reactor decompression (blowdown) and the initial period of emergency coolant application after blowdown when compared with the characteristics of normal power generation periods.

Of the above parameters, the dominant ones influencing fuel rod temperature distribution histories are gas gap thermal conductance (customarily an empirically based parameter including gas conductivity, and convective and radiative heat transfer coefficients within the gap) and fuel thermal conductivity. Both parameters have been historically obtained from experimental data and are not easily amenable to analytical quantification. Moreover, experimental observations frequently measure both quantities together -- so that their combined effect on heat transfer is easier to evaluate than their individual contributions (4, p. 10-1). However, the new AC require the calculation of gap conductance in accordance with the new requirements for evaluation models for each of the reactor manufacturers (60, p. 1104). The vendor's IAC approved evaluation models did not have the capability of calculating changes in fuel-clad geometry (i.e., swelling) during the LOCA, nor of evaluating the effect of thermal radiation across the gap during the same period. The new AC require that approved models incorporate these capabilities greatly enhancing the complexity of the calculation, but also improving its conservatism (at least in theory).

Ideally, for steady-state or accident conditions without fuel rod geometry changes, designers would prefer high values for both fuel thermal conductivity and gas gap conductance. Since the heat transferred from the fuel element is essentially directly proportional to the fuel conductivity and gap conductance, high values minimize stored heat, or, similarly, result in lower temperatures and gradients across the fuel rod for a given decay power output.

A6.1.1 Uranium oxide conductivity

Uranium oxide fuel does not have particularly high intrinsic thermal conductivity in comparison to other minerals. At normal operating temperatures, estimates of UO_2 thermal conductivities range from approximately 3 to 4 B-ft/hr-ft²-°F. Conductivities of this order

of magnitude are about the same as would be expected for relatively low density sedimentary rods such as sandstones. Fine grained igneous rocks (such as granite) may have conductivities as high as 20-30 B-ft/hr-ft²-°F, while relatively high conductivity metals (such as silver or gold) may have conductivities as much as two orders of magnitude greater than that of uranium oxide. The relatively low UO₂ conductivity contributes to the steepness of the steady state -- or zero time -- temperature profile of figure A6.2. The low conductivity also tends to retard the release of the stored initial energy during early periods when coolant heat transfer properties are relatively good, until later in the LOCA when poor fluid heat transfer exacerbates clad heating.

A6.1.2 Fuel-cladding gas gap conductance

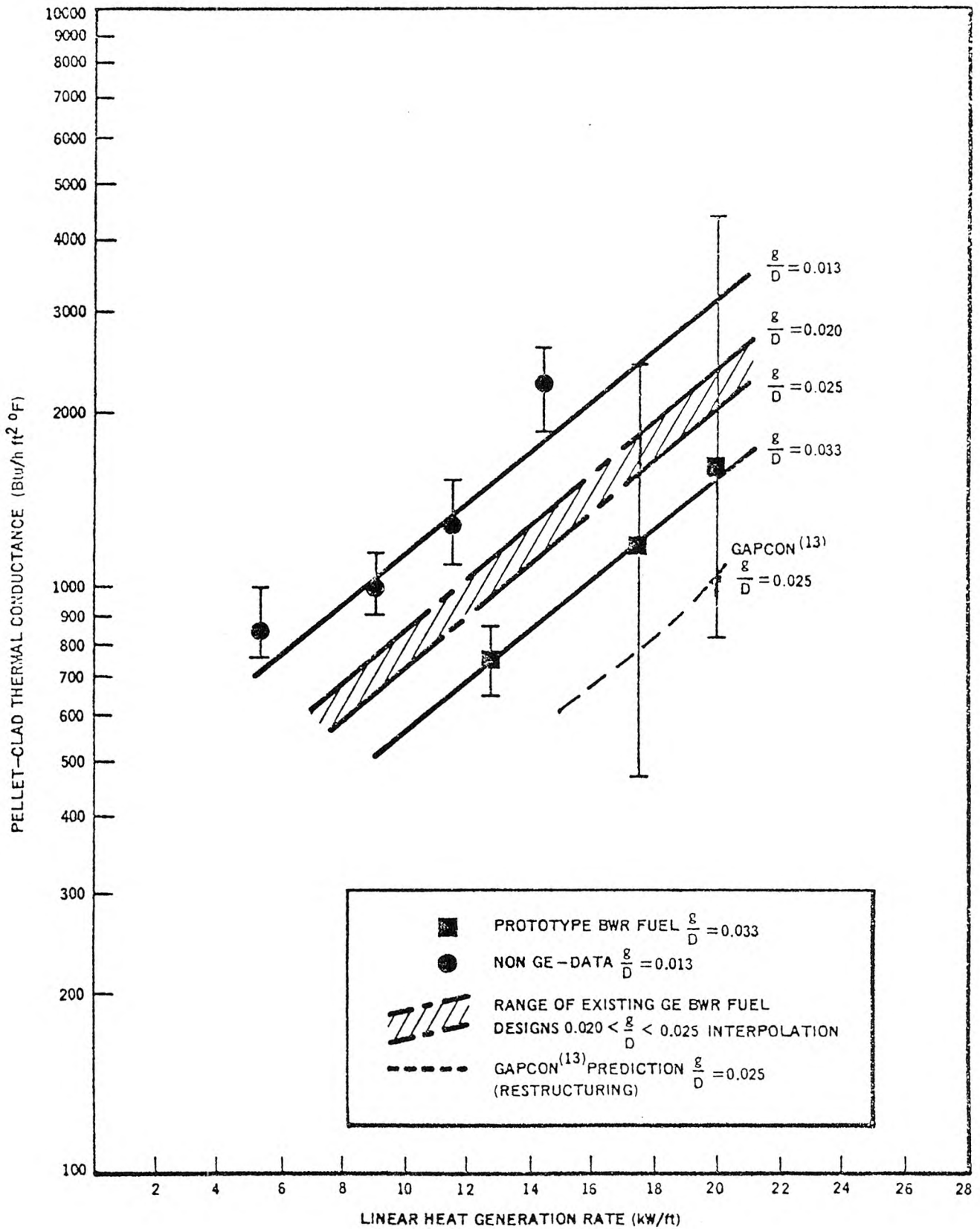
Gas gap conductance is strongly influenced by the dimensions of the gap between fuel and cladding and the physical composition of the gases filling the gap as well as the history of the operating power levels for the fuel. There is some uncertainty about the gap dimensions under normal operating and accident conditions. Initial cold gap overall dimensions are governed primarily by fabrication considerations on the part of the manufacturers and range from 7 to 12 mils (1 mil = .001 in). Most analyses of fuel heat transfer assume that the gap is uniformly distributed about the fuel elements, as indicated schematically in figure A6.1. Large gaps, on the order of the initial fabricated dimensions, and uniform gap distribution have been observed to produce relatively low gap conductances, on the order of 500-600 B/hr-ft²-°F when filled with the fission product gases associated with fuel rod end-of-life conditions (41). In practice, physical contact between fuel and cladding at many (or conceivably all) points along the rod is probable from the time of fabrication throughout the life of the fuel rod element. Fuel contact conditions are probably due to fabrication methods initially and fuel cracking, densification, and internal readjustment and cladding creepdown during operation (42).

Contact would raise gap conductance to values on the order of 1000 B/hr-ft²-°F. As a result of the mechanisms described above, gap closure has been observed by the vendors to occur relatively early in the fuel lifetime and to generally induce higher values of conductance throughout the normal operational lifetime of the fuel rods (36).

The physical composition of gases filling the fuel-clad gap also has a pronounced effect on the gap conductance. During fuel rod fabrication, relatively high conductivity gases such as helium are frequently used by manufacturers to provide initial fill and pressurization. The helium increases the start-of-life gas gap conductivity. After extensive neutron bombardment fission product gases dominate the gas composition with the rod. These gases have much lower conductivities, more on the order of argon than helium. Consequently, many experiments, conducted for short exposure periods, have used argon to simulate end-of-life conditions. Thus test results must be analyzed with care to assure that start-of-life gap dimensions are not combined with end-of-life gas conductivity so that applicability of results is obscured. After exposures of 30,000 MWd/MT, fuel cladding/steady state gap dimensions may be expected to be much smaller than at start-of-life, if in fact the gap has not been completely closed. The closing of the gap with extended exposure introduces a mechanism which tends to equalize gas gap conductivity over the lifetime of the fuel element, thus compensating for changes in the gas composition of the gap which tend to induce lower conductivities.

Averaged data from GE for gap conductances of BWR fuels are shown in figure A6.3 (42). Results shown are representative of measured conductances for fuel elements exposed in reactors for periods ranging from a few minutes to approximately 60,000 MWd/MT. The data is presented in terms of the parameter g/D , the ratio of the total gas gap dimension, g , to the cold fuel element diameter, D . As indicated by the error bars

Figure A6.3
BWR CLAD-GAP CONDUCTANCES



(After Figure 16, 42.)

in the figure, substantial scatter is observed in the data. But it should be noted that the greatest statistical variations occur for fuels with large initial gaps and low exposure times (42). It is to be expected that fuel contact variations for such fuel elements would be more pronounced than for elements with smaller g/D ratios or those exposed for longer periods where gap closing mechanisms would be more effective or have longer to operate.

The influence of the linear heat generation rate of normal operating conditions is shown very clearly in figure A6.3. Stored heat and decay power output are essentially proportional to the linear heat generation rate for the fuel rods. As the fuel is driven to higher linear power output (increased KW/ft), the gap conductance increases as the temperatures, heat output and stored energy increase. In the event of a LOCA, this mechanism for improved heat transfer with increasing power output from the fuel rods would help to assure that for the required DBA analysis conditions of high power output higher conductances would provide a feedback mechanism which would help to carry away the stored heat more rapidly than would be required under low power conditions.

The curve labeled GAPCON in figure A6.3 represents the results of application of the relatively untested numerical code GAPCON, favored by the AEC (4, p. 10-9) for prediction of the gap conductances. It can be seen that the results calculated by this method appear to be substantially lower (approximately a factor of two) than the average measured values for conductances.

A6.1.3 LOCA phenomena influencing fuel cladding heat transfer

Under blowdown conditions, with rapidly decreasing external core pressures on the fuel rods and rapidly increasing internal fuel rod pressures as gap filling gases increase in temperature, ballooning and rupture of the clad are probable. In fact, the Commission has

stated that,

. . . when the course of the LOCA is calculated according to the conservative prescription of an approved evaluation model, swelling and bursting of the cladding will be estimated to occur, in abundance (60, p. 1105).

Ballooning is expected to be a localized phenomena, with expansion occurring at rod hot spots. Individual fuel rod ballooning has been observed to occur over an axial length of only about one to two inches of the typical 12 ft axial length of the rod. Whether such a localized phenomena is significant when compared to heat transfer over the entire length of the fuel rod has not been seriously addressed in the literature. Though heat transfer locally would be inhibited substantially by reduction in gas gap conductances through gap expansion during ballooning (estimates indicate that gas gap conductance on the order of 10-100 B/hr-ft²-°F might be expected in the vicinity of the ballooned rod section (4, p. 10-21)), it is conceivable that axial heat transfer not presently included in calculational methods might contribute in an important manner to removing heat from the hot spot and preventing catastrophic local heat-up.

The AEC in its final proposals for resolution of the gas gap conductance problem seems intent on imposing demonstrably conservative calculation procedures. Under their new criteria, the influence of clad swelling and rupture and "any other applicable variables" would have to be accounted for in calculating gas gap conductance and consequently fuel rod heat transfer during a LOCA (60, p. 1104; appendix 3). Apparently, the locally reduced gap conductance due to clad ballooning would have to be used in calculating heat transfer to the hot rod in the reactor core (without regard to whether axial heat transfer was a significant contributor to local thermal conditions) in estimating whether the peak temperature criterion for the reactor has been exceeded.

To demonstrate the significance of the fuel rod thermal characteristics in influencing heat transfer during a LOCA, the AEC has reported the

results of a parametric investigation of the influence of gas gap conductance and external reflood and blowdown heat transfer coefficients on fuel rod thermal response (4, pp. 10-15 to 10-23). The results indicate that when ballooning occurs early in the transient, before blowdown is complete, gas gap conductance variation (over approximately an order of magnitude) can produce temperature increases of frequently as much as 100°F to 200°F. Gap conductances for these early ballooning cases were relatively low, ranging from about 10 to 100 B/hr-ft²-°F. If gap expansion occurs later in the LOCA, following blowdown, the results indicate that relatively high initial gap conductance coefficients (500-1000 B/hr-ft²-°F) result in lower stored energy in the fuel rod. Consequently, when ballooning occurs during reflood, producing lower gap coefficients over the same range of approximately an order of magnitude results in temperature decreases on the order of 100°F as conductances decrease. This rather surprising result demonstrates the importance of the gap conductance in storing thermal energy in the rod. If ballooning occurs early, low initial conductances result in the retention of high fuel temperatures and stored energy which, when released later during reflood, exacerbates the rod temperature history. If, on the other hand, the gap conductance is large during blowdown, fuel temperature and consequently stored fuel rod energy is reduced. Subsequent low gap conductances are then beneficial in reducing heat flow to the cladding at late times.

This example serves to demonstrate the difficulty in predetermination of what constitutes appropriate prescription of conservative values for fuel rod thermal property parameters. A simple statement that "low" or "high" values of gas gap conductance are always conservative from a safety standpoint cannot be supported. As a consequence, the AEC has concluded that pertinent fuel rod thermal parameters, presumably including conductivity and heat capacity as well as gap conductance, must be included or evaluated in the calculational models as function of time and temperature (60, p. 1104). Moreover, they have "required" that stored energy and gap

conductance must be evaluated on a case-by-case basis, as a part of individual plant licensing procedures (60, p. 1101).

A6.2 Evaluation of Stored Fuel Energy and Gas Gap Conductance Arguments

In the CNI concluding statements, the intervenors have pointed to the hearing record to provide confirmation for their claim that gaps exist in the understanding of the mechanisms of initial fuel energy storage and release and that uncertainties over the quantitative and qualitative effects of gas gap conductance and other fuel rod thermal parameters have contributed to a general lack of understanding of the ECCS problem. It is claimed that the neglect of the influence of clad ballooning on gap conductance in the Interim Acceptance Criteria prevented adequate computation of gap conductance and stored heat in any of the manufacturer's evaluation models. The hearing testimony of Morris Rosen, currently Technical Advisor to the Director of Reactor Licensing, is cited as evidence that specification of gap conductance and its influence on stored energy was neglected by the AEC until after the IAC were promulgated. CNI asserts that quantification of both the gap conductance and the conservatism of the calculation methods in which the conductances are applied (as well as other aspects of the problem) must be demonstrated before the conservatism of ECCS performance can be demonstrated (7, p. 4, 33-4. 34).

In the face of this criticism, the AEC appeared to progressively reevaluate the importance of the initial stored thermal energy problem and the physical parameters affecting it. In the AEC's initial direct testimony it was claimed that "peak cladding temperature has been shown to be relatively insensitive to changes in gap conductance during an accident" (8, p. 4.26). Consequently, gap conductance values recommended by the manufacturers were accepted apparently without sufficient examination. In the AEC's revised criteria, to become a modification of the Code of Federal Regulation (10 CFR Part 50), the sections of Appendix K

have been greatly strengthened which deal with subjects related to fuel rod initial stored energy and the influence of fuel rod swelling and rupture on gas gap conductance and other thermal parameters.

To indicate the extent of the AEC change in attitude, the verbatim text of the proposed changes to Appendix K, Sections IA1 and IB should be reviewed (appendix 3).

Though the AC modifications do not meet with the intervenors desired goals of quantifying specifically the thermal parameters of the fuel rod and/or the stored energy in the fuel, they have apparently been written to insure that conservatism in the calculation of the influence of stored energy and thermal parameters will be achieved. For example, Section IB requires the calculations of gap conductance take swelling and rupture into account so that they "are not underestimated." To this extent, the procedure provides a demonstrable area of conservatism.

Though no quantitative specifications for thermal parameters are given in the revised sections, the AEC argues that variations, even within a single vendor's reactors (especially in terms of gas gap conductance), are too great to permit specification of quantitative values within a general statement such as the proposed rules especially when new fuel designs have been recently introduced. Consequently they have argued that manufacturers' designs will have to be evaluated on a case-by-case basis to assure that the general criteria are being complied with and conservatism has been achieved in each case (60, p. 1102). Though this procedure may be considered undesirably ill-defined in the eyes of some critics, it does seem reasonable for criteria which might be expected to endure longer than a single reactor design without revisions.

Though the AEC may be faulted for having made a poor start in specifying the methods of treating fuel rod energy storage and release rates, generally speaking they seem to have made a strong attempt to incorporate intervenor (or perhaps ACRS) criticisms into the final AC so

that assured conservatism in energy storage calculations will be achieved. Final assurance of the ultimate conservatism of these calculations by manufacturers will now depend upon the capability of the AEC to maintain strict vigilance in reactor design review, on a case-by-case basis. The increased scope and depth of the criteria specifications will require corresponding increases in specialized investigatory expertise on the part of regulatory licensing personnel, and strengthened determination by the AEC to insist on total compliance on the part of the manufacturers.

Appendix 7 METAL-WATER REACTIONS, ENERGY RELEASE AND FUEL ROD
EMBRITTLEMENT

In the Interim Acceptance Criteria (see appendix 3), the first two general requirements were related to limitation of metal-water reactions between the zirconium alloy clad fuel rods and the high temperature steam produced during the LOCA. These requirements, limiting maximum calculated clad temperatures to less than 2300°F and overall clad-steam chemical reactions to less than 1 percent of total reactor cladding, had three implicit purposes: restricting energy release from the exothermic zirconium-steam reaction; limiting oxidation of the cladding to levels at which fuel rod embrittlement was not serious; and restricting the production of gaseous hydrogen from the reaction to safe levels. Hydrogen limitation was necessary in order to avoid potentially damaging explosive mixtures with air being developed when reaction products were swept through the coolant line break into the containment vessel. The simple 2300°F temperature limit of the cladding, without explicit exposure time at temperature limits, was the subject of much controversy for the AEC. In fact, the IAC's prescription on oxidation had little support from either industry or the AEC's laboratory associates.

As a direct result, the revised AC's most easily detectable changes are the replacement of the simple 2300°F temperature limit with two criteria. The first criterion reduced the allowed peak zircaloy temperature to 2200°F while the second provided a limit to the equivalent stoichiometric oxidation of the cladding to less than 17 percent of the total cladding thickness before oxidation (60, p. 1095; appendix 3). The criteria continue to retain the earlier 1 percent limit on overall chemical reaction with the total metal content of the cladding (excluding the plenum volumes) as a limit on hydrogen generation. The end result is a more definitive

Table A7.1 Potential Energy that Could Be Released in an Accident of the Indian Point-2 (PWR) or the Browns Ferry (BWR) Reactors

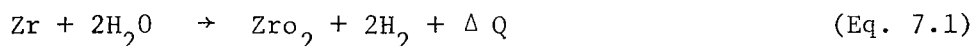
	2758-MW(t) Indian Point-2 Reactor		3293-MW(t) Browns Ferry Reactor ^a	
	Btu	MWhr	Btu	MWhr
Primary coolant internal energy	3.00×10^8	87.9	3.30×10^8	96.7
Hot reactor coolant system metal	0.18×10^8	5.3		
Core-stored heat			0.20×10^8	5.9
Core decay heat integrated for first 1/2 hr of accident	1.50×10^8	44.0	1.55×10^8	45.4
100% Zr-H ₂ O reaction	1.13×10^8	33.1	3.80×10^8	111.3
100% H ₂ -O ₂ reaction	0.88×10^8	26.0	3.10×10^8	90.8
1% Zr-H ₂ O reaction	0.01×10^8	0.3	0.04×10^8	1.1
1% H ₂ -O ₂ reaction	0.01×10^8	0.4	0.03×10^8	0.9

^aThe zirconium-water and hydrogen-oxygen reaction values for the Browns Ferry reactor are based on zircaloy both in the cladding and in the fuel-element shrouds.

set of criteria. However, as we shall show subsequently, their conservatism is not yet completely assured.

A7.1 Physics of the Zirconium-Water Reactions

The energy release, embrittlement hazard, and the hydrogen production all result from the following heat-producing (exothermic) reaction between the zirconium cladding and steam:



where

$$Q = 2912 - .0585T \quad (\text{B/lbm})$$

T = Reaction temperature (°F) at the oxide-unreacted
Zr interface

Equation 7.1 indicates that the products of the zirconium (Zr) and steam (H_2O) reaction are zirconium oxide (ZrO_2), elemental hydrogen (H_2) and heat (ΔQ).

As will be discussed in more detail later, it is possible for the local heat release rate to be of the same order of magnitude as the decay heat generation rate while the fuel rod cladding remains within the criteria temperature limits. It is instructive, however, to consider initially the time-oxidation reaction. Table A7.1 (43) is a representative of estimates which have been made to show that the heat release from the metal-water reaction is relatively small compared with other possible energy sources in the reactor for both PWRs and BWRs. Note that only if 100 percent of the Zr is reacted is the total heat release by the metal-water reaction, or a possible hydrogen-oxygen combustion process, comparable to either the primary coolant internal energy or the core decay heat released during the first half hour of the LOCA. Restricting the overall oxidation reaction to 1 percent or less of the total core cladding reduces the total energy release to values of the order of 1 percent of the decay heat for the same initial half hour period. The misleading part of the

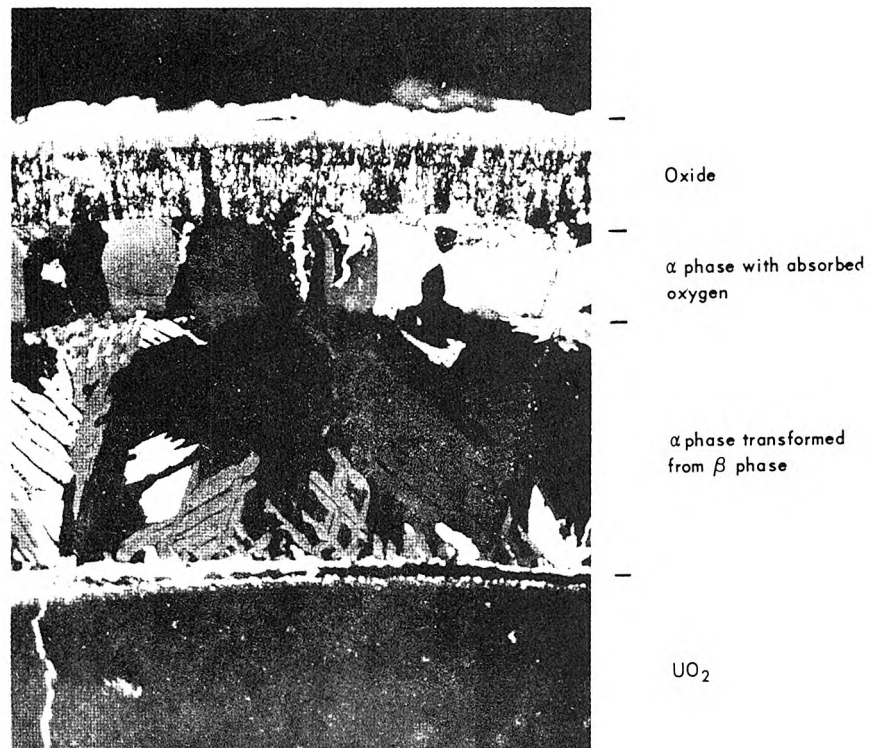


Fig. 2.1. Zircaloy-4 After Heating at 3.0°C/sec to 1600°C in Steam.
(From Ref. 17)

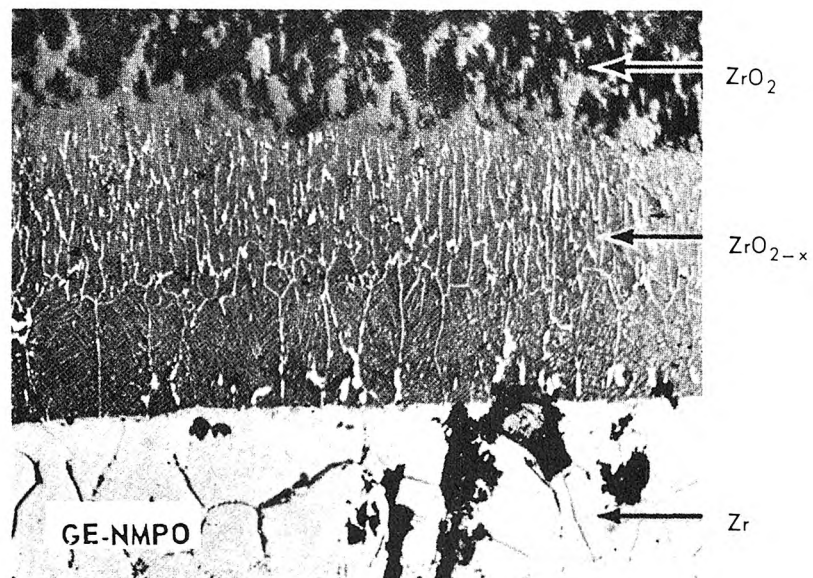


Fig. 2.2. Zircaloy-4 After Heating at 0.3°C/sec to 1300°C in Steam.
(From Ref. 16)

Figure A 7.1 Photomicrographs of Zr oxidation forms.

presentation in table A7.1 is that it compares time-integrated total energy release for all reaction products for the LOCA with the resulting implication that the metal-water energy release under IAC limits was essentially negligible. More appropriately, comparisons should be made between transient energy release rates for the oxidation reaction and decay heat at the hot spots of the reactor core.

The embrittlement of the zircaloy cladding is produced as a result of crystalline transformation in the metallic structure of the cladding associated with the zirconium oxidation process. The clad oxidation takes place by a diffusion process in which several identifiable crystalline transformations occur as the zirconium progresses from pure metal to ZrO_2 . In a typical clad oxidation process, an outside layer of pure, stoichiometrically complete ZrO_2 is formed. At greater clad depths, beneath the zone where the chemical oxidation reaction has gone to completion, $ZrO_{2-\alpha}$ crystals are formed. This customarily clearly defined, but incompletely oxidized, layer is referred to as alpha zirconium. Beneath the oxygen stabilized alpha phase material, a layer of zirconium is customarily found in which the oxygen concentration is low and the crystalline form essentially unchanged from the unoxidized state (beta phase). Because the ZrO_2 and the α -phase zirconium are characteristically brittle at low temperatures, the oxygen content of the ductile β -phase zirconium and its relative thickness compared to the initial thickness of the cladding are apparently the controlling factors in fuel rod embrittlement.

A reproduction of photographs of two typical sections of oxidized cladding is shown in figure A7.1 (43). As implied in equation 7.1, upon complete oxidation to ZrO_2 zirconium experiences a weight gain of approximately 35 percent. When combined with a density decrease of 14 percent (from 6.49 g/cc for Zr to 5.68 G/cc for ZrO_2), the end result of the transformation from Zr to ZrO_2 is a physical volumetric growth of approximately 54 percent. This volumetric growth combined with a natural tendency for α -phase Zr to separate along crystal boundaries,

as indicated in figure A7.1, induces severe embrittlement in the outermost layers of oxidized cladding.

At elevated temperatures, a relatively small amount of oxygen (from 5 to 10 percent atomic) can be absorbed in the β -phase zirconium before the material becomes oxygen saturated without substantial crystalline alteration and consequent reduction in ductility. As saturation is approached, precipitation of α -zirconium in the grain boundaries and solid-solution hardening of the β -phase zirconium can apparently take place over a rather critical temperature range (on the order of 2200°F to 2300°F) the degree of precipitation and/or solid-solution hardening is apparently exacerbated by high temperatures attained during oxidation and/or slow cooldown. Both of these processes are extremely important to β -phase ductility.

Apparently, the degree of oxygen saturation of the β -phase zirconium is the controlling factor in fuel cladding embrittlement. However, the exact mechanisms by which it takes place, and the adequacy of theoretical and empirical models of oxygen uptake in the zirconium and the relative embrittlement induced thereby, are not felt to be thoroughly quantified or understood. As indicated in their Concluding Statement, the AEC staff did not believe that the state-of-the-art would permit adequate assessment of oxygen uptake in the β -phase (6, p. 86). In the absence of adequate models, the AEC has attempted to prescribe a low enough limit on the maximum cladding temperature to assure that oxygen uptake was maintained below embrittlement limits.

Concern over embrittlement was thus the principal reason behind the current revision of the maximum temperature criterion to 2200°F. As stated by the Commission in their technical discussion of the new AC, "Our selection of the 2200°F limit results primarily from our belief that retention of ductility in the zircaloy is the best guarantee of its remaining intact during the hypothetical LOCA" (60, p. 1098). The adequacy of this limit will be discussed subsequently.

A7.2 Reaction Rates

The zirconium oxidation rate is controlled by the solid state diffusion of the ionic reaction products through the oxide layer. Generally the chemical reaction rate is of parabolic form (25, p. 2-7), i.e.,

$$\text{where } w^2 = K_p(T)t$$

where w = weight of metal reacted (converted to equivalent quantities of ZrO_2), mg/cm^2 of surface area

t = exposure time, (sec)

$K_p(T)$ = parabolic rate constant, a function of temperature (T), $(mg/cm^2)/sec$

Based upon experimental results, a number of expressions for the parabolic rate constant, K_p , have been derived. One of the most widely used relationships was derived by Baker and Just (44). Their fit was based upon experimental data for molten zirconium droplets reacting with water. Their derived rate constant relationship is given by:

$$K_p(T) = 33.6 \times 10^6 \exp \left(\frac{K_a}{RT} \right), \ln \left(\frac{mg^2}{cm} \right) / sec \quad (Eq. 7.3)$$

where

K_a = activation energy, = 45,000 cal/mole

R = gas constant = 1.987 cal/mole-°K

T = metal temperature, °K

The Baker-Just relationship, Eq. 7.3, with its single temperature independent expression for the activation energy, K_a , and the rate constant, K_p , is consequently best fit to reaction rates near the zirconium melting point.

Based upon empirical observations of changes in zirconium oxide crystalline form during temperature excursions, a better fit to

the temperature-reaction rate data with three separate activation energies was derived by H. H. Klepfer (45). The fit of the Klepfer equations is given by:

$$K_p(T) = 5.52 \times 10^4 \exp(-29,000/RT), \left(\frac{\text{mg}^2}{\text{cm}^2} \right) / \text{sec} \quad (\text{Eq. 7.4})$$

for $\sim 20^\circ\text{C} < T \leq 890^\circ\text{C}$;

$$= 3.58 \times 10^5 \exp(-33,500/RT)$$

from $890^\circ\text{C} < T \leq 1577^\circ\text{C}$;

$$= 1.04 \times 10^{11} \exp(-79,800/RT)$$

from $1577^\circ\text{C} < T < 1852^\circ\text{C}$ (Zr melting point).

The Klepfer fit to the reaction rate-temperature data is compared with the Baker-Just equation results in figure A7.2. Note that in the Klepfer equations the changes in activation energy, K_a , are associated with phase changes in the crystalline structure of the zirconium oxide at the indicated temperatures.

It should be observed that for most of the temperature range of interest, $T \leq 2300^\circ\text{F}$, the Baker-Just relationship gives a reaction rate which is conservatively higher (from a safety design standpoint) than the Klepfer fit. Only for temperatures less than 1900°F , where the reaction rate is approximately an order of magnitude below its value at 2300°F , does the Baker-Just relationship depart from conservatism.

Differentiating equation 7.2 and substituting the Baker-Just relationship for the reaction rate constant (equation 7.3) and the energy-mass relationship of equation 7.1, yields equations relating the reaction rate as a function of the local energy released by the reaction to the thickness of zirconium oxidized in the reaction. These relationships are:

$$\frac{d\delta}{dt} = \frac{0.0616}{\delta} \exp \left(\frac{-41,000}{T} \right), \text{ in/sec} \quad (\text{Eq 7.5})$$

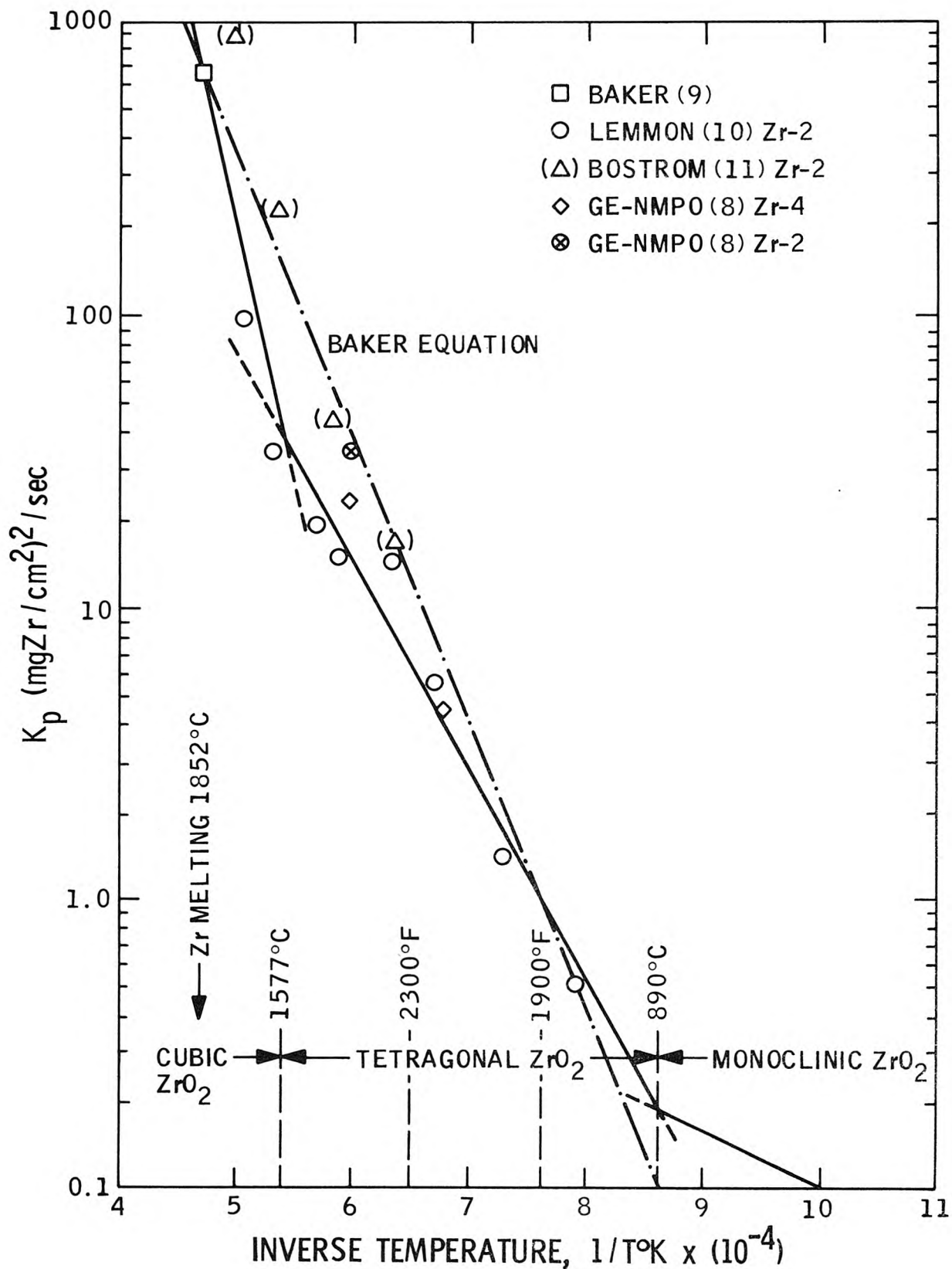


Figure A7.2 Reaction Rate of Zirconium as a Function of Temperature
(After Figure 2-7, 25, by permission.)

where:

δ = equivalent thickness of metal reacted (inches)

t = time (sec)

T = temperature of the metal ($^{\circ}\text{R}$)

and

$$q = \frac{5.33 \times 10^7}{\delta} \exp \left(\frac{-41,000}{T} \right), \left(\frac{B}{ft^2 - hr} \right) \quad (\text{Eq. 7.6})$$

where

q = reaction energy release rate

These equations demonstrate the negative feedback - self limiting effect of oxide thickness on the rates of energy release and growth rate of oxidized zirconium. That is, for a given temperature, the growth rate of oxidized material and the reaction energy release rate decrease with increasing thickness of the oxidized layer (or equivalently with increasing time). This effect is an important factor in energy release rates in the metal-water reaction and embrittlement induced in the course of a LOCA.

A7.3 Energy Releases

Based upon equation 7.6, a comparison of local energy release rates with fission product decay power output is shown in figure A7.3. The curves are shown for highly idealized cases of assumed constant temperature reactions (over all times) as a function of the initial oxidized thickness at the beginning of the temperature excursions. It may be observed that a more realistic LOCA temperature history would have a more gradual increase in temperature to its maximum value, rather than the assumed constant temperature cases shown here. However, the curves can be used to evaluate a more realistic excursion when it is recognized that they indicate relative values of reaction power to decay power which would be attained when the temperature reaches the levels shown for the equivalent oxidized thicknesses of zirconium shown.

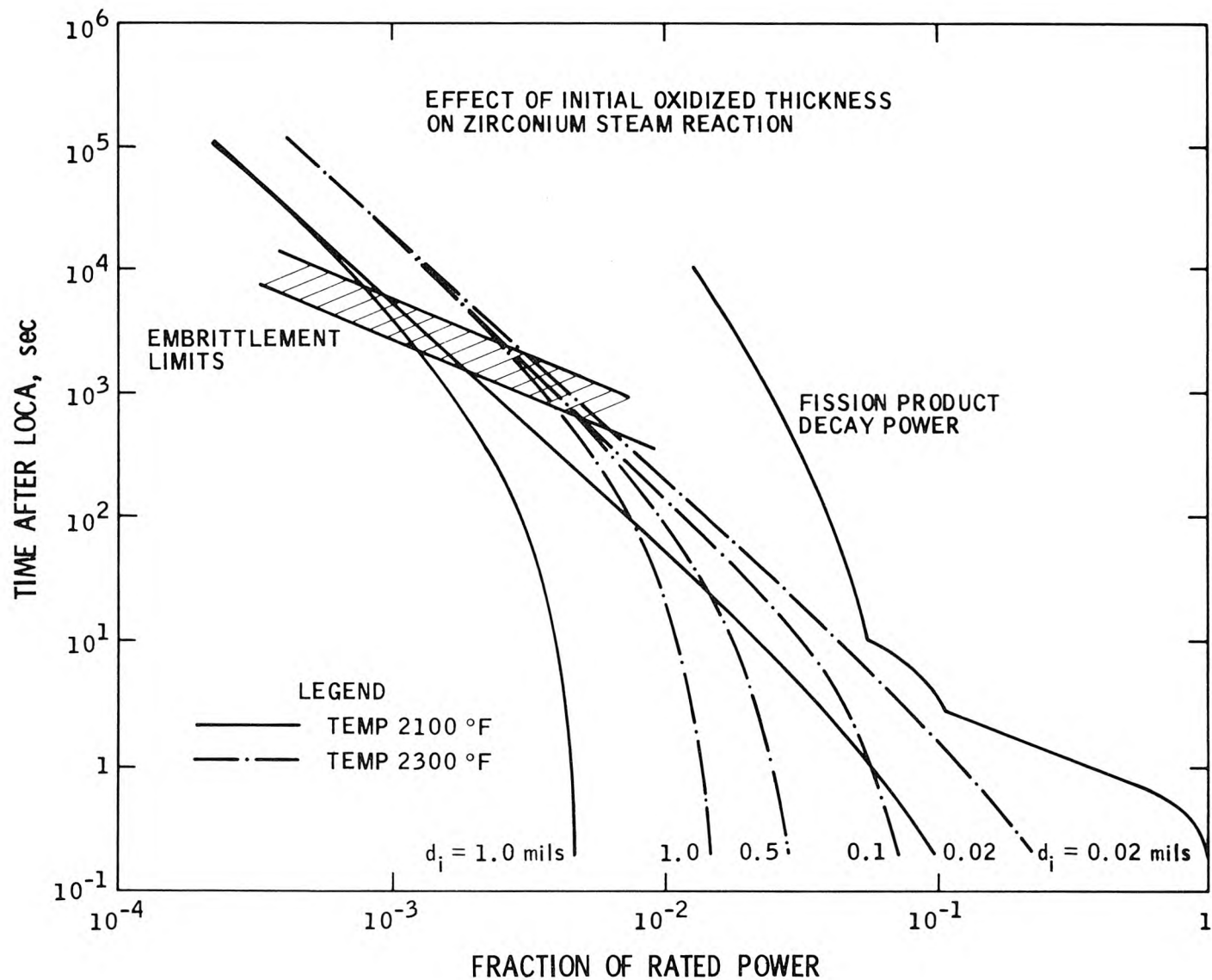


Figure A7.3 Comparison of Local Metal-Water Energy Release Rates with Fuel Rod Fission Product Decay Power Output as a Function of Equivalent Oxidized Thickness.

For example, if the development of a peak 2300°F temperature is delayed for approximately ten seconds for cladding with an equivalent oxidized thickness of .02 mils, the equivalent energy output rate would be identical to that at the initial times shown in figure A7.3 (.01 sec). Under these circumstances, the local metal-water reaction power output would exceed the fission product decay power output of the fuel rod at ten seconds after LOCA initiation by nearly a factor of ten.

The message of the figure is clear. At a maximum permissible temperature of 2300°F, the relative energy flux from the zirconium-water reaction is not negligible in comparison with energy release rates from the fission product decay power input of the fuel rod. However, from the beginning of the hearings, the AEC regularly downgraded the significance of the zirconium-water reaction energy release rate. In their initial direct testimony they stated,

For calculated LOCA temperature transients limited to 2300°F, the rate of energy release from Zircaloy-water reactions is always substantially less than the decay heat rate. However, if the cladding temperature could reach 2800°F, the Zircaloy-water energy release rate would exceed the decay heat rate except for the longest transients. If the cladding temperature could reach 2500°F, the cladding water energy release rate would equal the decay heat rate only during unrealistically rapid transients. The cladding temperatures calculated by the evaluation models take into account the contribution of the cladding-water reaction energy (8, p. 2-6).

It can be seen from figure A7.3 that the original AEC conclusions were not easily defensible. To assure that reaction energy release rates are approximately an order of magnitude below the decay heat rate, maximum temperatures would have to be limited to about 1800°F or less, or initial oxide thicknesses would have to be of the order of one mil (3 to 4 percent of the unoxidized clad thickness).

For long transients, the equivalent oxide thickness term (δ) in the denominator of Eq. 7.6 causes the reaction energy rate to decay like $t^{-1/2}$. For such extended transients, the rate of decrease in the energy release rate from the zirconium-water reaction (for a fixed temperature reaction) is substantially more rapid than the decrease in the fission product decay heat rate, as can be seen in figure A7.3. Consequently, after periods on the order of an hour, assuming temperatures are controlled within IAC standards, the relative energy release from metal-water reactions is low enough to be essentially insignificant. However, the first several minutes of elevated temperatures (in excess of about 2000°F), will induce energy releases comparable to the decay heat rate during the transient period. If the time to peak temperature (2200°F) is retarded, peak metal-water energy release rates may even exceed decay heat output temporarily, as discussed in the earlier example.

It should be noted, however, that all calculations of the energy release based upon the Baker-Just reaction rate relationships will conservatively predict metal-water reaction energy release rate. Though early AEC statements appeared to minimize the significance of metal-water reactions as an energy source, the IAC approved vendor evaluation models required the use of the conservative Baker-Just rate relationships. Consequently, energy release rates calculated under the IAC were adequate. In the new AC, the Commission has explicitly recognized the metal-water reaction rate as a significant source of heat which must be evaluated in a conservative fashion. The new AC go beyond the IAC specifications, requiring that the reaction cannot be assumed to be steam limited and must be evaluated for internal clad reactions, if the cladding is calculated to swell and burst. Under these circumstances, it appears that calculated energy release rates from metal-water reactors should be conservatively accounted for under the AC.

A7.4 Embrittlement

The AEC's primary concern in establishing maximum temperature limits (and finally quantitative limits on the extent of the Zr-steam reaction) has been over the embrittlement aspects of the effects of oxidation. This has been the AEC position since the publication of the IAC. Modifications in AEC embrittlement assessment have resulted primarily from problems associated with defining the physical mechanism for embrittlement and selecting a method for quantifying the criterion for limiting the oxidation to levels which would give conservative assurances of ductile fuel rod behavior throughout the LOCA.

Over the period of time of the ECCS hearings, the criteria defining acceptable embrittlement limits have changed as new experimental data became available. In the AEC's initial direct testimony, the defense of the simple 2300°F limit was based on a relatively thinly defended argument. Embrittlement was "classified" as occurring if the ZrO₂ layer reached 16-18 percent of the total original clad thickness. This, it was simply stated, corresponded "to a thickness of ZrO₂ plus αZr of about 40% of the clad thickness" (8, p. 2-3).

It was also recognized that clad ductility was a function of not only the peak temperature experienced, but also the reduced temperatures after the thermal excursion had been limited. Studies at ORNL (46) were cited as evidence that zero ductility temperatures (ZDT) (the temperature below which cladding has no remaining ductility and will suffer brittle failure under relatively light loads) would be below 1000°F as long as the equivalent clad temperature transient were held to less than: "6 minutes at 2400°F, 10 minutes at 2300°F, approximately 15 minutes at 2200°F, and about 27 minutes at 2200°F" (8, p. 2-4). No substantial discussion was given to the adequacy of a ZDT of 1000°F. It was implied, however, that since none of the then currently accepted evaluation models indicated temperatures exceeding 2300°F, or excursions

above 2300°F for more than 3 minutes, "reasonable assurance" was provided that "significant cladding failure should not occur as a result of oxygen uptake" (8, p. 2-5).

The failure of the simple 2300°F maximum clad temperature limit alone to explicitly limit oxidation was, however, recognized by the AEC in its initial direct testimony. It was stated:

While the criterion does not specifically include a time-at-temperature limitation, this limitation is implicit in that the evaluation models used for calculating the temperature history are also specified. We have not observed, from the results of calculations performed by the reactor manufacturers or from the results of our own calculations, prolonged temperature transients that approach those wherein the clad may enter non-ductile state (8, p. 2-5).

The need for an explicit limit on exposure duration as well as peak temperature was emphasized by the intervenors in their initial direct testimony, and acknowledged by all of the manufacturers and utility participants as a desirable change in the criteria (4, pp. 18-3 to 18-5).

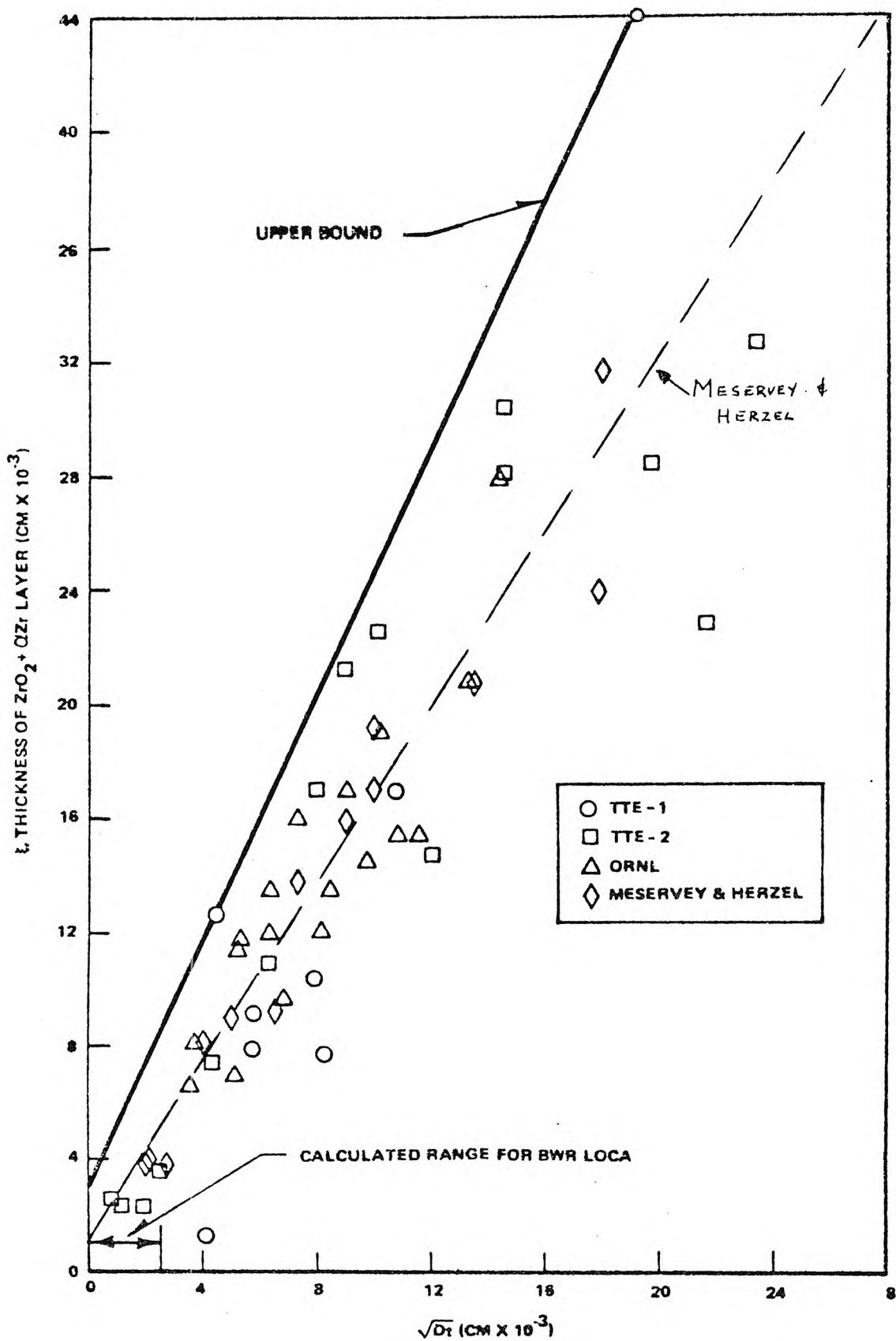
Several methods were proposed for relating the moving boundary between the relatively highly oxidized α -phase zirconium and slightly oxidized β -phase to embrittlement characteristics of the fuel rods. One of the methods, which is typical of several others, is that of Meservey and Herzel (47). They exposed zircaloy tubing to steam for various temperature histories and arrived at the results shown in figure A7.4 (25). After simplification, the theoretically based relationship fitting their data analysis reduced to:

$$\xi = 1.6 \times 10^{-3} \sqrt{Dt} + 0.001 \quad (\text{Eq. 7.7})$$

ξ = measured thickness of ZrO_2 + αZr layer (cm)

D = diffusion coefficient, $= 0.916 \exp \left[\frac{-41,000 + 1500}{RT} \right]$

Figure A7.4
Oxide Layer Thickness vs. \sqrt{Dt}



(After Figure 2-12, 25 by permission.)

t = time (sec)

R = universal gas constant, 1.987 cal/mole - °K

T = sample temperature, (°K)

The upper bound of the data, a more conservative basis for estimating the extent of oxidation, is given by:

$$\xi = 2.19 \times 10^{-3} \sqrt{Dt} + 0.0028 \text{ (cm)} \quad (\text{Eq. 7.7a})$$

Several concepts were advanced by the AEC as methods of quantifying the extent of embrittlement as a function of the degree of fuel rod oxidation. In the AEC's Supplemental Testimony, a quantitative method for relating ZDT to the thickness of oxidized material was suggested (4, p. 18-16). The depth of fully and partially oxidized material ξ_t (the total thickness of ZrO_2 and α -phase Zr including both inside and outside cladding surfaces) and conversely the relative thickness (F_w) of "unoxidized" β -phase Zr were related to the ZDT in the AEC model as:

$$\text{ZDT} = 2727 - 3636 F_w (^\circ\text{F}) \quad (\text{Eq. 7.8})$$

where,

$$F_w = \frac{W - \xi_t}{W} = \frac{1 - .874 \xi_t/w_o}{1 + .126 \xi_t/w_o}$$

W = "as oxidized" clad thickness

w_o = initial unoxidized clad thickness

Assuming oxidation was limited to 16 percent conversion to equivalent ZrO_2 (relating α and β -phase material to ZrO_2 through atomic oxygen content by means of zirconium oxidation phase diagrams --e.g., 25 figure 2.4) the AEC has stated that $\xi_t/w_o = .56$, approximately, for these conditions. Under these assumptions, equation 7.8 indicates that the ZDT for 16 percent conversion to equivalent ZrO_2 is about 1000°F. Sixteen percent equivalent oxidation was still stated to be sufficiently

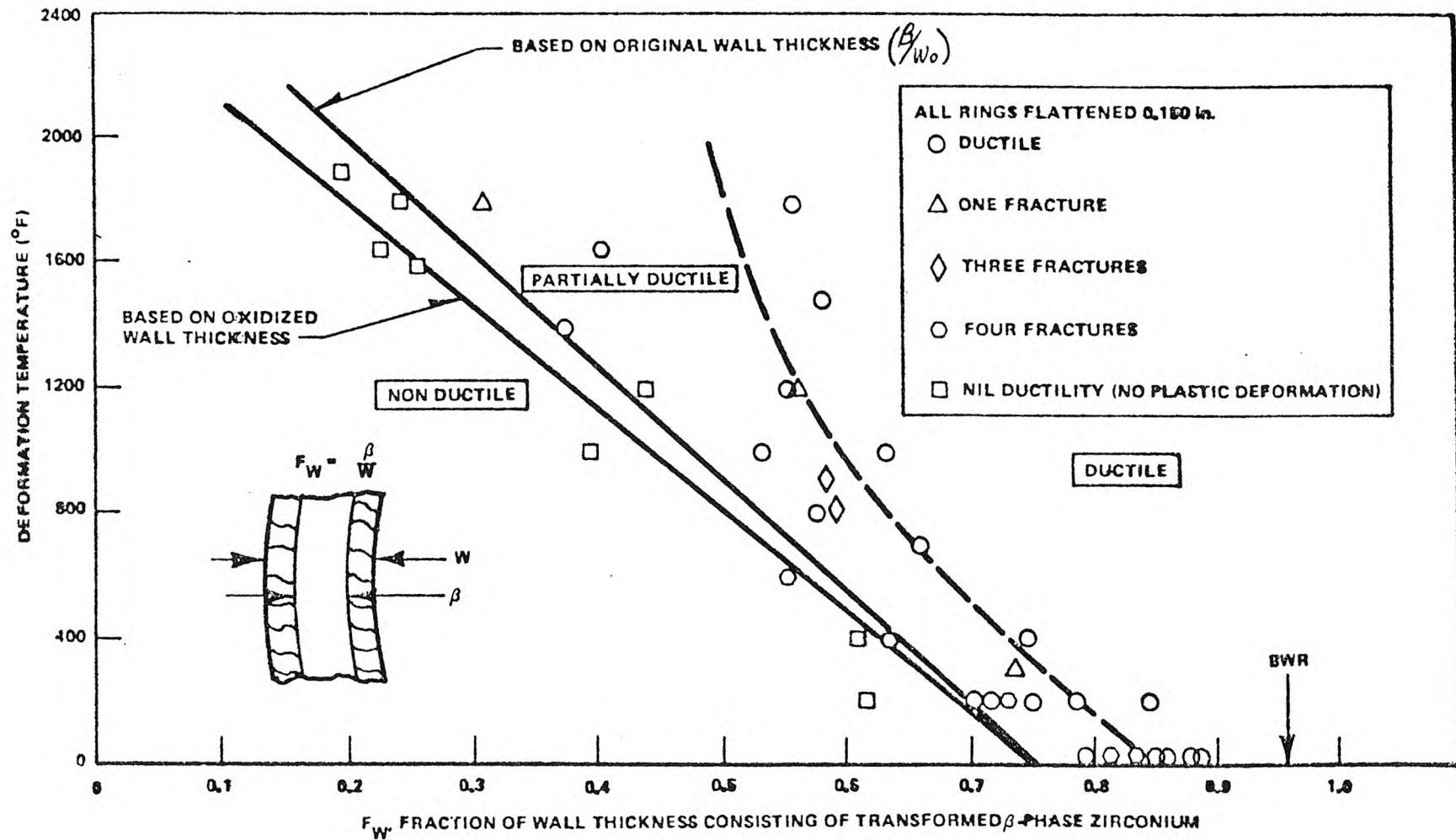
small to survive quench loads under these circumstances.

The question of the relevance of quench loadings, as opposed to other loading mechanisms which might be more closely related to fuel rod loads during other LOCA periods (as well as experimental problems associated with definition of physical parameters during quench), led to investigation of other ways of relating embrittlement to oxidation. The results of an ORNL investigation of the effects of deformation temperature for impact and compression load tests on oxidized zircaloy cladding are shown in figure A7.5 (48). The results are shown as a function of the fractional wall thickness (F_w) of transformed β -phase zirconium. The results indicate that as F_w decreases, or as more of the cladding is oxidized to ZrO_2 and α -phase zirconium, the ZDT, or temperature of departure from ductility, increases. When F_w is of the order of 0.2, the temperatures at which non-ductile failures occurred (approximately 2000°F) are almost the same as the limiting maximum allowable temperatures of the IAC (2300°F). At these oxidation levels, brittle failure could occur shortly after temperature turnaround -- long before the fuel rods were cooled to steady-state or safe conditions.

Figure A7.5, however, does not relate the results of the tests to maximum exposure temperatures for the fuel rod test specimen nor the length of exposure time. Figure A7.6 presents the same test results in terms of maximum exposure temperatures and exposure times. The results generally indicate the parabolic temperature relationships implied by equation 7.2. Though there is a fairly substantial scatter in the data, the results indicate a general trend of decreasing ductility with increasing exposure time. The straight-line, constant temperature-time curves have been "eyeballed" through the data only to show the general indication of the trend of F_w with time. The relatively poor correlation between F_w and exposure time at a given temperature indicated by the scatter in the data is indicative of the problems of using parameters such as F_w or ξ_t/w_o to relate oxidation to interface

Figure A7.5

Ductility vs. Deformation Temperature



(After Figure 3-8, "Specimen Ductility as a Function of Deformation Temperature and Fraction of Wall Thickness (F_W) Consisting of Transformed β -phase Zirconium," 25, by permission.)

A7-20

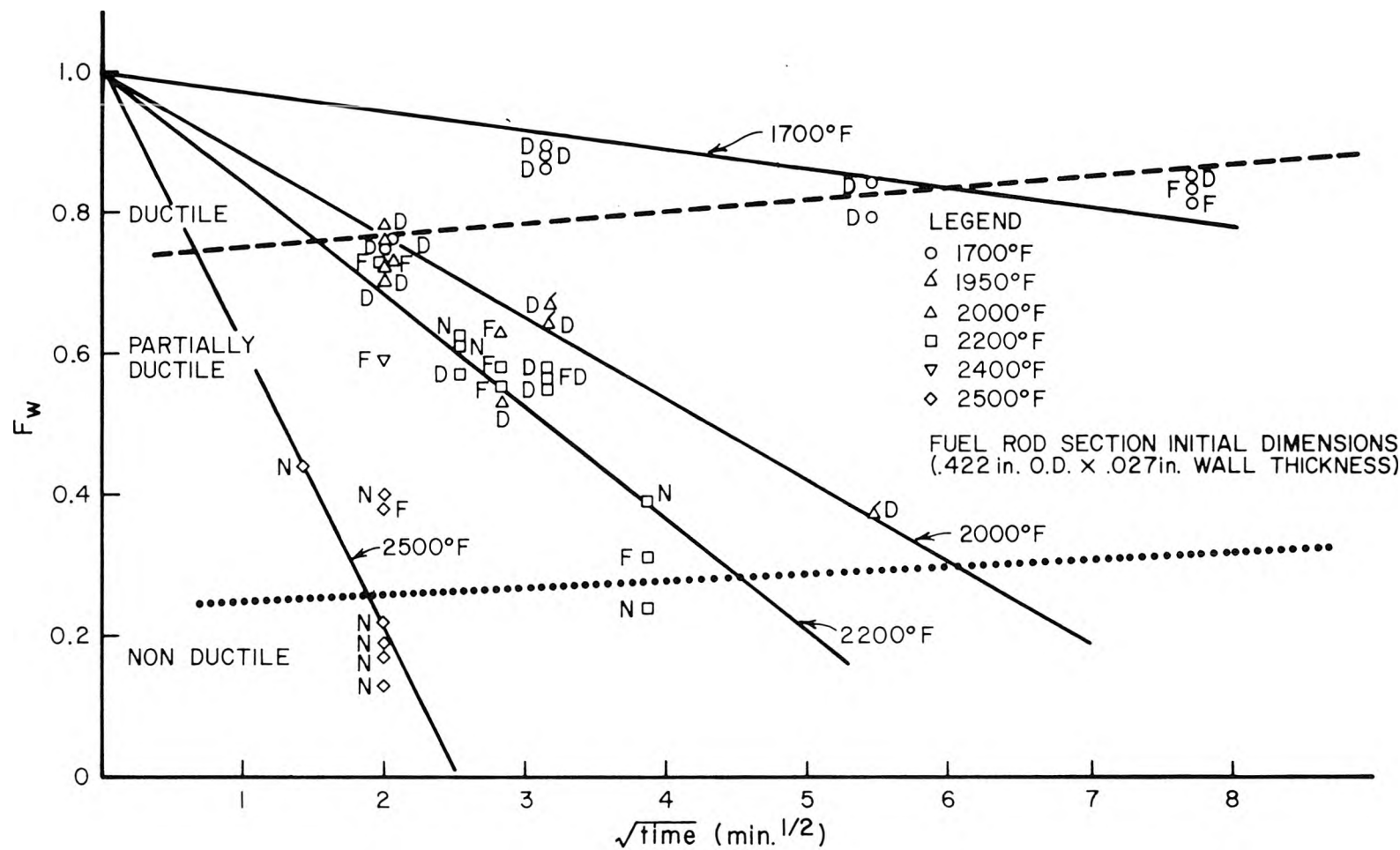


Figure A7.6 Beta Phase Wall Thickness (F_w) as a Function of Zircaloy - Water Reaction Time For Constant Temperature Exposure

thickness of affected regions in the fuel rod. In spite of the scatter in the data, the results indicate that ductility is not assured for values of F_w much less than 0.8. In preparing its Concluding Statement, the AEC reviewed the methods which were recommended by the hearing participants for evaluating oxidation limits for embrittlement criteria. The results, presented in table A7.2, indicate the principal features of the recommendations (6, p. 88). After reviewing the proposed methods for evaluating the location of the moving α -phase zirconium boundary through methods relating F_w or ξ to temperature and time, the AEC concluded that the multiplicity of methods for calculating these parameters could not be correlated with high confidence. They compared the results for the various methods proposed and concluded, that using an equivalent 17 percent clad reacted criteria (using the Baker-Just equations for total oxidation of material) bounded the results of other methods with satisfactory conservatism. Comparison of the recommendations indicates that the 17 percent limit permits more oxidation than the equivalent oxygen uptake implied by the methods recommended by Combustion Engineering, Westinghouse, and the Utility Group. The C. E. proposed limit ($F_w > .65$) corresponds to a limit of about 10 to 14 percent equivalent ZrO_2 oxidation. As indicated in table A7.2, Westinghouse recommended an oxidation limit equivalent to a 16 percent clad reaction. In their Concluding Statement, the Utility Group recommended,

. . . a limit on the calculated . . . equivalent oxidation of 12 mole percent would prevent clad embrittlement and failure and should conservatively bound conditions which could be experienced during a design basis LOCA (22. p. 39).

Moreover, it should be observed that the 17 percent clad reaction limit corresponds to an F_w of about 0.5. As shown in figures A7.5 and A7.6, oxidations of this extent generally result in relatively high ZDT values, and reduce the fuel rod to partially ductile to non-ductile conditions during cooling periods.

A7.5 Commentary

As indicated in the foregoing sections, the AEC position with respect to the metal-water reaction has evolved steadily during the

Table A7.2

RECOMMENDATIONS FOR EMBRITTLEMENT CRITERIA AND METHODS

Participant	Source	Temperature Limit °F	Oxidation Limit	Inside Reaction	Clad Thinning Percent Expansion	Zirc-Water Reaction Equation
Utilities	Conclusions	2500	12-17% clad reacted	0-75% of outside	0-40%	Klepfer
B & W	Conclusions	2400	19% clad reacted	0	0	Klepfer
G.E.	Conclusions	2300*	None*	0	0	Baker-Just
C.E.	Conclusions	2500	Fw > 0.65	2/3 of outside	50%	C.E.
Westinghouse	Conclusions	2700	$\xi t/w_o < 0.47^{**}$	0	0	Westinghouse
Staff	Rebuttal	2200	$\xi t/w_o < 0.44$	100% Baker- Just after rupture***	Calculate	Baker-Just
Staff	Conclusions	2200	17% clad reacted	100% Baker- Just after rupture***	Calculate	Baker-Just

* G.E. believes 2700°F and 17 percent reaction are better limits but does not recommend any change from Interim Acceptance Criteria.

** Westinghouse states that this is equivalent to 16 percent clad reaction.

*** Within 1.5 inches of the center of the rupture.

the hearings. Under pressure from the intervenors, ACRS, and manufacturers, the AEC finally acknowledged the necessity of providing a specific limit to the reaction in addition to peak temperature. Using the Baker-Just oxidation relationship, the newly prescribed 17 percent equivalent clad reaction criterion now allows the designer to uniquely specify the limiting metal-water reaction for his reactor. The AEC's original position, that the time history of the reaction was implicit in the calculational models of the reactors, overlooked the principal problem that the oxidation was not explicitly limited by a peak temperature criterion alone (8, p. 2-5).

Initially the intervenors argued that the 2300°F peak temperature limit alone was simply nonconservative. In addition to pointing up the need to rectify the criteria's failure to specify explicit temperature-time relationships, the CNI proposed that the following additional criteria changes should be incorporated relative to metal-water oxidation reactions to increase conservatism.

- (1) A minimum ZDT of 200°F or less should be required.
- (2) Based upon calculated outside oxidation thicknesses, equal oxidation should be assumed on interior rod surfaces to allow for metal-water reactions on surfaces which had ballooned and ruptured during blow-down.
- (3) Moreover, to account further for the effect of fuel rod swelling, the calculated excursion should assume that cladding walls were thinned to one-half the original thickness during the expansion.

The CNI concluding arguments were tempered somewhat. Their call for the above proposed criteria changes was not repeated, perhaps because they felt that the AEC was moving to incorporate explicit

changes which were close to their initial criticisms. They did state, however, that the single peak temperature limit without some form of explicit time restriction had been "totally discredited." They also stated that before meaningful criterion could be established, it would be necessary to conduct more experimental work -- especially in areas of thinned and ruptured tubing and the verification of calculated LOCA forces on the fuel rods (7, p. 5-49).

More experimental work in fuel rod oxidation and embrittlement would seem to be desirable. The basis for most of our current embrittlement limits seems to rest upon less than 100 individual experimental measurements. This is not a particularly large set of data upon which to base an extremely important criterion, especially considering the rather large scatter in the data. However, it does seem that the data should be adequate to permit conservative oxidation limits to be established. Consequently, though additional tests may be desirable, the available information appears sufficient for establishing criterion limits.

From the perspective of energy release (e.g., figure A7.3), the AEC's revised 2200°F peak temperature limit appears to potentially permit non-negligible zirconium-water reaction energy release rates when compared to local fission product decay heat release. Depending upon the time the peak temperature for the rod is reached, the Baker-Just relationship would predict energy release rates which are nearly equal to (and possibly greater than) the fission product decay power. Though the total energy release is small when the temperature excursion is held within the criteria limits (table A7.1) -- on the order of 1 percent of the decay heat release -- the local transient heat release rate is substantial and could adversely effect the thermal history at the core hot spot. If this energy source were neglected in the ECCS thermal analysis, the resulting positive feedback energy input mechanism

could conceivably spread the hot spot over broader core regions with potentially disastrous consequences. Reduction of this problem to metal-water energy release rate levels of essentially inconsequential magnitudes, e.g., about an order of magnitude below critical decay heat power levels, would require a corresponding reduction of peak temperatures to approximately 1800°F. Alternatively, consideration of intentional utilization of an initial oxide thickness of about 0.5 to 1 mil (or equivalently, an initial equivalent 2 percent to 4 percent cladding oxidation) would also reduce the peak metal-water reaction power levels to approximately an order of magnitude below decay heat levels with the current 2200°F limit. A reduction in permissible metal-water reaction to power levels of this magnitude would effectively eliminate this contribution as a significant energy source. There is no reason to believe, however, that the energy associated with the metal-water reaction is not adequately predicted in all of the evaluation models meeting the new AC. The Baker-Just reaction rate equations prescribed by the criteria appear to be adequately conservative in specifying energy released and oxidation occurring as a result of the reaction. Calculations based upon the Baker-Just relationships, and which satisfy the other AC prescribed metal-water criteria, should adequately include this energy source in their analyses. If the resulting calculations indicate that the cladding thermal response remains with the 2200°F AC limit, there should be no basis for concern that the metal-water reactions have not been conservatively treated from an energy source standpoint.

From an embrittlement point of view, the proposed 17 percent equivalent oxidation limit, based upon analyses using the Baker-Just reaction rate method, appears to be of borderline conservatism (e.g., figures A7.5 and A7.6). The 17 percent equivalent oxidation, as previously noted, corresponds approximately to $F_w = 0.5$. Under these circumstances, oxidized rods could be only partially ductile and at

temperatures of less than about 900°F could be below the ZDT. With the scatter in oxidation depths indicated by the data of figure A7.6, and consequent uncertainty in relating embrittlement to equivalent percent oxidation or to actual measured oxidation penetration, a 17 percent oxidation limit appears to provide only borderline conservatism. The C. E. recommendations of $F_w > 0.65$, corresponding approximately to an equivalent 10 to 14 percent cladding reaction limit, with an estimated ZDT of approximately 400°F, would appear to be a considerably more conservative (and comfortable) operating limit. The consolidated Utilities Group has also recognized the need for a more conservative 12 percent limit, as previously cited.

With respect to oxidation limits on the inside and outside surfaces of the fuel rod, the AEC's ultimate resolution of this criteria omission in the IAC appears to be reasonable. According to the new AC, if in the course of the LOCA cladding rupture is calculated to occur, the inside tube surfaces shall be included in the oxidation calculation, beginning at the time of calculated rupture. Moreover, the criteria require that all evaluation models shall include a model for predicting clad swelling rupture, which is based on "applicable data in such a way that the degree of swelling and incidence of rupture are not underestimated" (60, p. 1104). Adequate application of these criteria should meet the requirement of most reasonable men for conservative treatment of the metal-water reaction due to clad rupture.

Rather than incorporating a specific quantitative statement of required fractional clad thinning in sections where swelling and rupture have occurred, as proposed by CNI, the final AC require that the evaluation model provide for calculation of the ballooned clad thickness at the elevation of the rupture. The criteria require that the equivalent unoxidized clad thickness be based upon the initial cladding cross-sectional area,

taken at a horizontal plane at the elevation of the rupture, if it occurs, or at the elevation of the highest

cladding temperature if no rupture is calculated to occur, divided by the average circumference at that elevation. For ruptured cladding the circumference does not include the rupture opening (60, p. 1095).

Though classical rupture experiments do not demonstrate such idealized thinning conditions for swelling and rupture, the assumption of an "average" distribution of material, in accordance with the revised criterion specifications, appears reasonably conservative.

Generally speaking, though absolute conservatism of the metal-water reaction criteria may not be assured within the AEC's Concluding Statement, the revised criteria have gone a long way towards eliminating most of the principal initial objections of the intervenors (but at the same time implicitly acknowledging that many of the objections were fundamentally legitimate). Though clad ductility may not conservatively be assured by the criteria, the most pertinent data on oxidation reactions appear to have been considered. The AEC attempts to arrive at a consensus appear to have been unsuccessful, and vendor recommendations have apparently been misinterpreted. Combustion Engineering's recommendation of a minimum F_w of 0.65 was interpreted as being equivalent to a brittle layer thickness ratio of $\xi / w_o = .47$ (60, p. 1097). This stated equivalency is incorrect. The ratio $F_w = .65$ corresponds to $\xi / w_o = .37$, which corresponds to a fractional equivalent oxidation of 10 to 14 percent, as previously discussed. Thus the "uniformity of opinion" which the Commission has sought to establish is not particularly evident, nor does it strongly support the 17 percent oxidation limit of the AC.

Assuming assured conservatism is desirable, a reduction in the limiting equivalent oxidation to 12 percent of the initial clad thickness would appear to be needed to achieve this goal.

Appendix 8 FLECHT TEST PROGRAMS

The conservatism of the modeling of heat transfer physical processes for reactor core reflooding and core spray emergency coolant mechanisms, as they were defined by the IAC, has been questioned by both the ACRS and the CNI. Because the physical processes occurring during coolant application are quite complex, the principal means of developing modeling tools for their evaluation has been through empirical methods. Attempts have been made to isolate and examine many of the elements of the LOCA physical processes through a number of experiments. To evaluate the phenomena of reflooding and core spray, the Full Length Emergency Cooling Heat Transfer (FLECHT) test programs were conducted. The FLECHT programs have formed the basis for the post-blowdown heat transfer models prescribed in all of the AEC criteria to date. Though there have been some modifications in interpretation of the program results over the course of the hearings, the basic evaluation model methodology is still fundamentally dependent upon the validity and adequacy of the FLECHT data. This appendix will review the several criticisms which have been raised by the intervenors with respect to these aspects of the FLECHT tests.

A8.1 General FLECHT Test Description

Two separate test programs were conducted, one for boiling water reactors (BWR-FLECHT) and another for pressurized water reactors (PWR-FLECHT). The two test programs, conducted by GE and Westinghouse respectively, under subcontract to the Idaho Nuclear Corporation, though different in procedural detail were similar in many general ways. For uncertain reasons, GE's BWR-FLECHT program was singled out for more extensive criticism by CNI than the PWR-FLECHT. Consequently, in order to evaluate the CNI criticisms, much of the material in this chapter has been directed toward review of the BWR-FLECHT program.

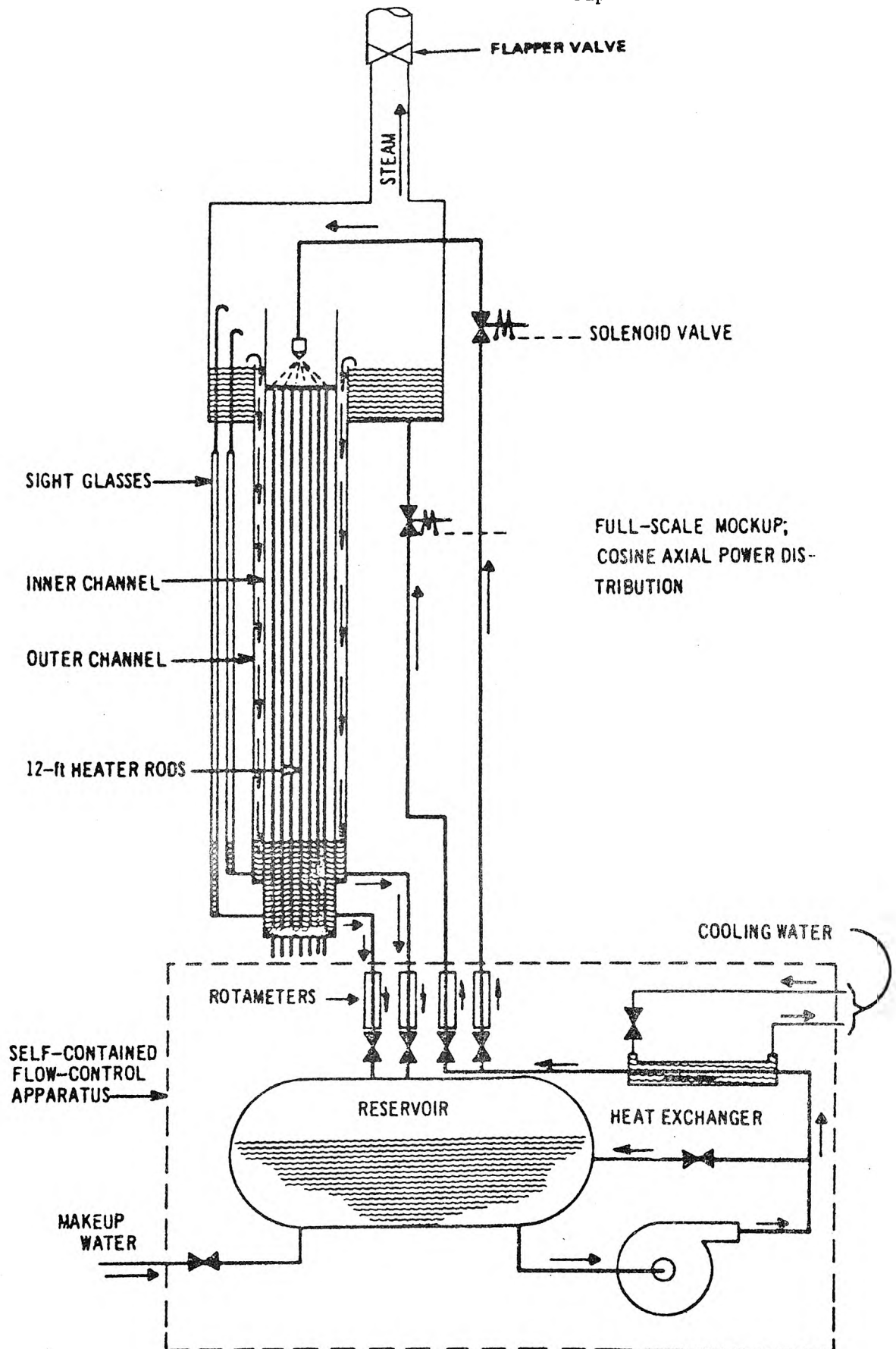
Figure A8.1 is a schematic diagram (49) of the BWR-FLECHT test setup. As indicated in the figure, full length fuel rods were tested in a 7 x 7 rod bundle configuration closely approximating BWR reactor bundles which were contemporary to the test period. For the tests, the fuel rod cladding material was fabricated of either stainless steel or zircaloy. The rods were heated by electrical resistance heaters designed to mock up operation rod axial power distribution (a chopped cosine power distribution along the axial length of the rod). Typical heater construction for Westinghouse PWR-FLECHT rods (50) is shown in figure A8.2. Though heater materials and construction details differed somewhat between the BWR and PWR programs, general elements of rod and heater designs were similar for both programs. The time history for the electrical power supplied to the rod bundles was programmed to simulate bundle decay heat in an operating reactor with reactor operating power at LOCA shutdown as a parameter.

The tests were conducted in a parametric fashion. Analyses of calculated values of specific coolant application rates, initial temperatures, peak operating power, and time sequences of coolant application were the basis for determination of the ranges for each of these parameters. Table 8.1 (49) represents a summary of the BWR-FLECHT test program indicating the number of tests, rod materials and parameter ranges utilized in the program. A similar number of tests was conducted in the PWR-FLECHT program.

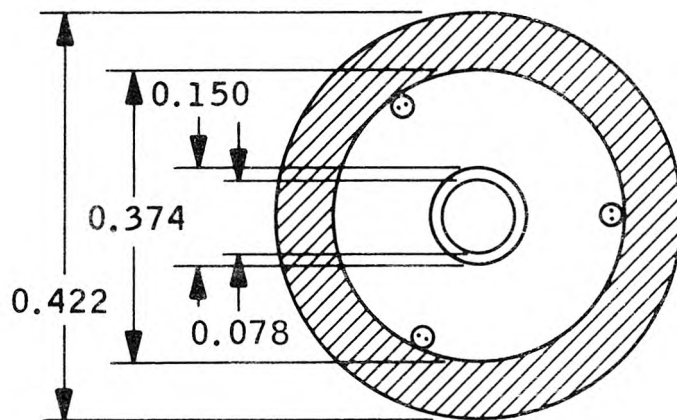
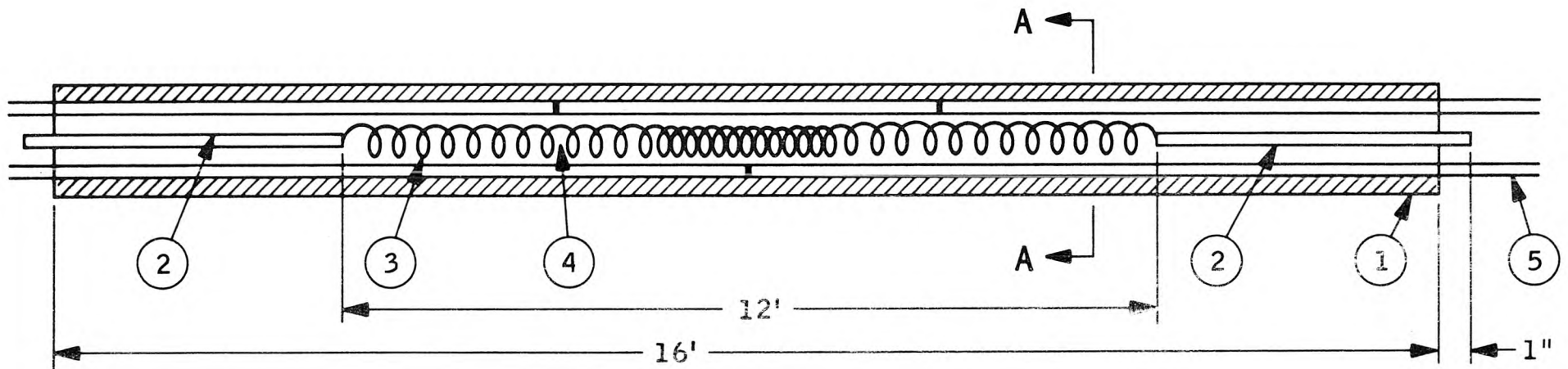
A8.2 FLECHT Test Program, Analysis

Three principal criticisms have been made of the BWR-FLECHT tests. The first complaint was that although all BWR fuel rods are manufactured of a zirconium (Zr) alloy, zircaloy, only 5 of the 143 FLECHT tests utilized Zr rods. The remaining 138 tests were conducted with stainless steel (SS) rods. Since, as discussed in appendix 7, Zr reacts exothermically with water at elevated temperatures, contributing

Figure A8.1 BWR-FLECHT Test Setup



(After Figure 1, 49.)



SECTION "A-A"

1. TUBE 0.422" OD BY 0.024" WALL TYPE 347 STAINLESS OR ZIRCALOY-4
2. NICKEL CONDUCTOR 0.132" DIA x 36" LONG
3. NICHROME OR KANTHAL RESISTANCE WIRE 0.036" DIA x 420" LONG COILS 0.15" OD (COSINE DISTRIBUTION ALONG LENGTH)
4. BORON NITRIDE INSULATION SWAGED DENSITY 2 gm/cm³
5. CHROMEL ALUMEL THERMOCOUPLES WITHIN 0.040" STAINLESS SHEATH

Figure A8.2 FLECHT Heater Rod Schematic Design
(After Figure 2.4, 50, by permission.)

Table A8.1
BWR--FLECHT Testing Summary

Bundle Testing Date	Type of Tests	Tests	Peak Power (kW)	Coolant Rate	Initial Temperature (°F)	Reference
SS1N July 68	Flooding	5 Steady State	20-235	To Hold Level	--	GEAP-10117
		16 Transient	240-390	0.6-3.7 ips	1328-2150	
SS2M Aug-Sept 68	Spray	4 Steady State	200-250	2.1-2.6 gpm	--	GEAP-10092
		15 Transient	120-390	1.1-3.35 gpm	1120-2050	
SS3M Sept-Dec 68	Spray	4 Steady State	200	0.6-2.1 gpm	--	GEAP-10092
		38 Transient	120-390	0.4-6.5 gpm	810-1450	
Zr1M May 69	Spray	1 Transient	200	2.45 gpm	1790	GEAP-10092
SS2N Aug-Oct 69	Spray	3 Steady State	150	1.0-2.45 gpm	--	GEAP-13088
		24 Transient	100-250	2.45-5.0 gpm	865-1850	
		8 Combined	250-235	2.0-3.5 gpm	1335-1870	
		Spray & Flooding		2.0-6.0 ips		
Zr2K Dec 69	Spray	1 Transient	195	2.45 gpm	1920	NEDG-13064 GEAP-13112
Zr3M Mar 90		1 Transient	240	2.45 gpm	2345	GEAP-13174
Zr4M May 70	Spray	1 Transient	240	2.45 gpm	2298	GEAP-13174
Zr5M June 70	Spray with Flooding	1 Transient	300	3.25 gpm	2325	GEAP-13174
				6.0 ips		
SS4N Sept-Oct 70	Spray Flooding	10 Transient	250	2.45 gpm	1076-1718	GEAP-13190
		11 Transient	250	1.5-6.0 ips	1300-1600	

5 Zircaloy-Clad Bundle Spray Transient Tests
11 Stainless Steel-Clad Bundle Steady State Spray Tests
95 Stainless Steel-Clad Bundle Spray Transient Tests
5 Stainless Steel-Clad Bundle Steady State Flooding Tests
27 Stainless Steel-Clad Bundle Flooding Transient Tests

additional energy to that of the decaying fission products, the application of water to the core has the potential of increasing the heat input to the fuel rods rather than cooling them, as desired. The small number of Zr tests in comparison with the total test program was seriously faulted by the CNI.

There are two basic explanations which have been proffered for the relatively large number of SS rod bundles used in the programs. The first deals with test repeatability and economics and the second with simplification of the analysis of the physical processes of the bundle cooling mechanisms. The repeatability and economics explanation is the reason most emphasized by the AEC and GE (6, p. 166; 60, p. 1123).

The FLECHT program was conducted as a parametric test series which strongly affected the economics issue. Because rod heaters are hand wound, their individual response characteristics are relatively unique. The SS rods were apparently chosen primarily for their durability. They could be used repeatedly in testing (for 30 or 40 individual tests) without substantial changes in response over the series. Thus, differences in heater characteristics would not influence the test series results.

On the other hand, as a result of metal-water reactions, Zr rods could be used only once and then had to be subjected to a destructive post-mortem examination after the test. Since metal-water (M-W) reaction rates are inversely proportional to the oxidation thickness, repeated tests on used rods could not be expected to be reproducible with respect to M-W reactions as the oxidized depth would increase with each test. Moreover, destructive testing of the rods was felt to be the only "absolute" method of evaluating the extent of the M-W reaction which took place in the test. Consequently, to improve test reliability and to reduce cost of rod fabrication, SS rods were used most extensively in the test program.

The second reason for using more SS than Zr rods involves the problems of simplifying heat transfer analyses by separating the M-W reaction from the physical processes of cooling rods which were not undergoing an A-W reaction. It was assumed that the M-W reaction was an independent heat input mechanism to the fuel rods, separable from the basic heat transfer processes of cooling. On this basis, the SS rods permitted direct determination of the applicable heat transfer coefficients for the cooling mechanisms without supplementary heat input complications. The validity of this concept of separability of the two heat transfer mechanisms rests on the assumption that the radiative and convective heat transfer processes for heat transmission between fuel rods and the coolant fluid are essentially independent of the fuel rod materials, and thus are functions primarily only of temperature and fluid flow conditions. Thus, it was felt to be possible to evaluate heat transfer coefficients from SS tests where the results would not be affected by M-W reactions. The purpose of the Zr tests was then to evaluate the validity of these assumptions by using SS derived heat transfer coefficients to evaluate (or provide post-test predictions) of the thermal response of Zr bundles.

The weakness of these arguments for rod material selection is that because of the small number of Zr tests and the poor quality of the Zr results, questions remain concerning the validity of the assumptions of the equivalence of non-reactive heat transfer characteristics for the two materials and the legitimacy of decoupling the metal-water reaction from the clad heat transfer mechanisms. Thus the AEC in its Concluding Statement argued that,

...the stainless steel bundle tests were performed so as to obtain parametric heat transfer information, whereas the Zircaloy-clad bundles were used to determine whether any significant anomaly existed in the transient heat transfer behavior of Zircaloy (6, p. 166).

The Commission concluded in their discussion of the AC that,

Stainless steel was used instead of zircaloy as the cladding material for nearly all of the FLECHT tests because it is more durable under the test conditions. Although it is not usual to expect significant differences in convective heat transfer coefficients from different solid material surfaces, the possibility of such differences was considered, perhaps resulting from such factors as differences in thermal conductivity and differences in wetting properties. The reasonable conclusion was reached that the effect of the difference between zircaloy and stainless steel, if any, would be small. There is a difference, of course, in the rate of heat generation from steam oxidation, but this is deposited within the metal under the surface of the oxide film. The presence of this heat source should not affect the heat transfer coefficients, which depend on conditions in the coolant outside the rod.

The few FLECHT runs made with zircaloy clad rods provide uncertain and conflicting evidence. Westinghouse pointed out that all of the zircaloy runs except one (run 9573) yield higher heat transfer coefficients than were obtained with steel (Westinghouse Concluding Statement, pp C-74 to C-76; Exhibit 150, pp 3-98 & ff). Consolidated National Intervenors pointed out that most of these runs were made at unreasonable high flooding rates, and that a different result was obtained from run 9573 where the flooding rate was about one inch per second. In the first 18 seconds of this run, before multiple heater rod failures occurred, the zircaloy clad rods heated up faster than predicted from the stainless steel based correlations (Exhibit 1041, pp 6.7 & ff). This anomalous result has been attributed to experimental error, or possibly to an unusually skewed initial temperature distribution along the length of the rod (Exhibit 1113, pp 17-6-17-7).

On balance, the Commission sees no basis for concluding that the heat transfer mechanism is different for zircaloy and stainless steel, and believes, that the heat transfer correlations derived from stainless steel clad heater rods are suitable for use with zircaloy clad fuels rods. It is apparent, however, that more experiments with zircaloy cladding are needed to overcome the impression left from run 9573 (60, pp. 1123, 1124).

These conclusions appear to be basically valid. Non-reactive heat transfer mechanisms have been studied in great detail for heat exchanger applications in many industries. These studies have shown that convective heat transfer mechanisms are fundamentally related to the fluid properties in the heat exchanger -- and only in a second order manner to the properties of the metallic surfaces of the heat exchanger itself, assuming equivalent surface roughness, etc. The decoupling of the reactive and convective heat transfer mechanisms is a less clear cut proposition. The evidence for this assumption appears to be somewhat uncertain, under the circumstances. However, the assumption of decoupling itself appears to have been legitimate at least initially. In view of the uncertainty and conflicts in the Zr test results, the AEC position that more Zr tests are needed appears entirely valid.

The second major criticism of the BWR-FLECHT tests concerns the excessive use of molybdenum heating elements to power the rods during reactor LOCA transient simulation. As a resistance heater material, molybdenum (Mo) has a very large temperature coefficient of resistivity which induced substantial amounts of power shifting in the rods as a result of the "chopped cosine" axial power distribution designed for simulation of in-core heat distribution in the tests and from rod-to-rod as a result of inter-rod thermal interactions. The net result was that pre-test predictions of power distribution for the rods were poorly correlated with measured values for tests utilizing Mo heaters.

Fortunately for ECCS designers, some of the FLECHT tests were conducted using other heater materials. Chief among the alternates was Nichrome V. The temperature coefficient of resistivity for Nichrome V is nearly constant. However, only a limited number of tests were conducted using this heater material because of its low failure temperature (2500°F). Consequently, according to GE, the heaters could

not be used in tests requiring extended operating periods at temperatures greater than 2000°F (51, p. 6). Consequently Mo heaters were favored, particularly in the SS tests, where the same rod bundle was repeatedly used for evaluation of different test parameters.

Examination of the GE test documents does not seem to demonstrate that the substantial Mo induced power shifting observed in the FLECHT tests was initially expected. Mechanisms for controlling the positive feedback from the Mo resistivity characteristics do not appear to have been incorporated into the test apparatus until the last 3 Zr tests were conducted, nearly two years after the program began (52, p. 4). Moreover, no clear evidence can be seen that individual rod power determinations were (or perhaps could be) made to allow for systematic analysis of test results. Consequently, the Mo heaters seem to have contributed painfully complicating factors and little useful information to the FLECHT tests.

As a consequence of the complications associated with interpretation of Mo heater test results, GE has leaned heavily upon results obtained with Nichrome V heaters in evaluation of heat transfer coefficients for their transient analysis methods (52, p. 5). Results obtained with Mo heaters are only rarely referenced and then primarily for the limited number of Zr test results in which they were used. For example, the heat transfer coefficients utilized in the GE core spray and reflood calculation model (54, p. 58), were derived on the basis of the SS2N test series (53, p. 26), in which Nichrome heating elements were used for the rod bundles -- as indicated by the N in the descriptive number scheme (SS2N) for the test series. Thus, tests conducted with heater materials with low temperature coefficients of resistivity have had a disproportionately large influence on assessment of heat transfer mechanisms when compared to the actual number of tests conducted in the FLECHT program.

The third principal criticism of the BWR-FLECHT program concerns the non-reproducibility of the tests. CNI presented (9, p. 5.26) results from tests conducted with SS2N and SS4N-49 rod bundles which showed that for selected cases, peak temperatures for the SS4N tests were approximately 15 percent greater than reported SS2N results for similar test conditions. In their BWR-FLECHT Final Report, GE used heat transfer coefficients derived from SS2N results to compare with temperature-time histories obtained in tests of zircaloy-clad bundles (52, appendix A). As shown in figure A8.3 using SS4N test results, the CNI derived heat transfer coefficients which when applied to the limited number of Zr test results shown in the CNI direct testimony, showed "better" correspondence with the measured values presented than the results shown for GE's SS2N derived heat transfer coefficients.

It should be noted, however, that only four time histories were shown by the CNI in which the correlations were presented. In every case, data reported by the CNI shows GE predicted peak temperatures which fall short of the measured data. These results are obviously biased towards support of the CNI contention that the GE heat transfer coefficients are inadequate. In actuality, using the methods recommended for design purposes by GE (using 100 percent of the predicted M-W reaction), predicted temperatures were greater than the measured temperatures for 37 of 44 measurements for tests Zr2K, Zr3M, Zr4M (52, appendix A). Needless to say, comparison of CNI derived heat transfer results would not have been as convincing for these 37 other sets of results, if they had been shown.

A comparison of the test and predicted results is shown in the graphs, figures A8.4 to A8.6, prepared by GE showing analytical predictions for all the Zr tests based upon a 50 percent Baker-Just M-W reaction energy input to the heat transfer calculation compared to measured peak temperatures and turnaround times (52, pp. 73-75).

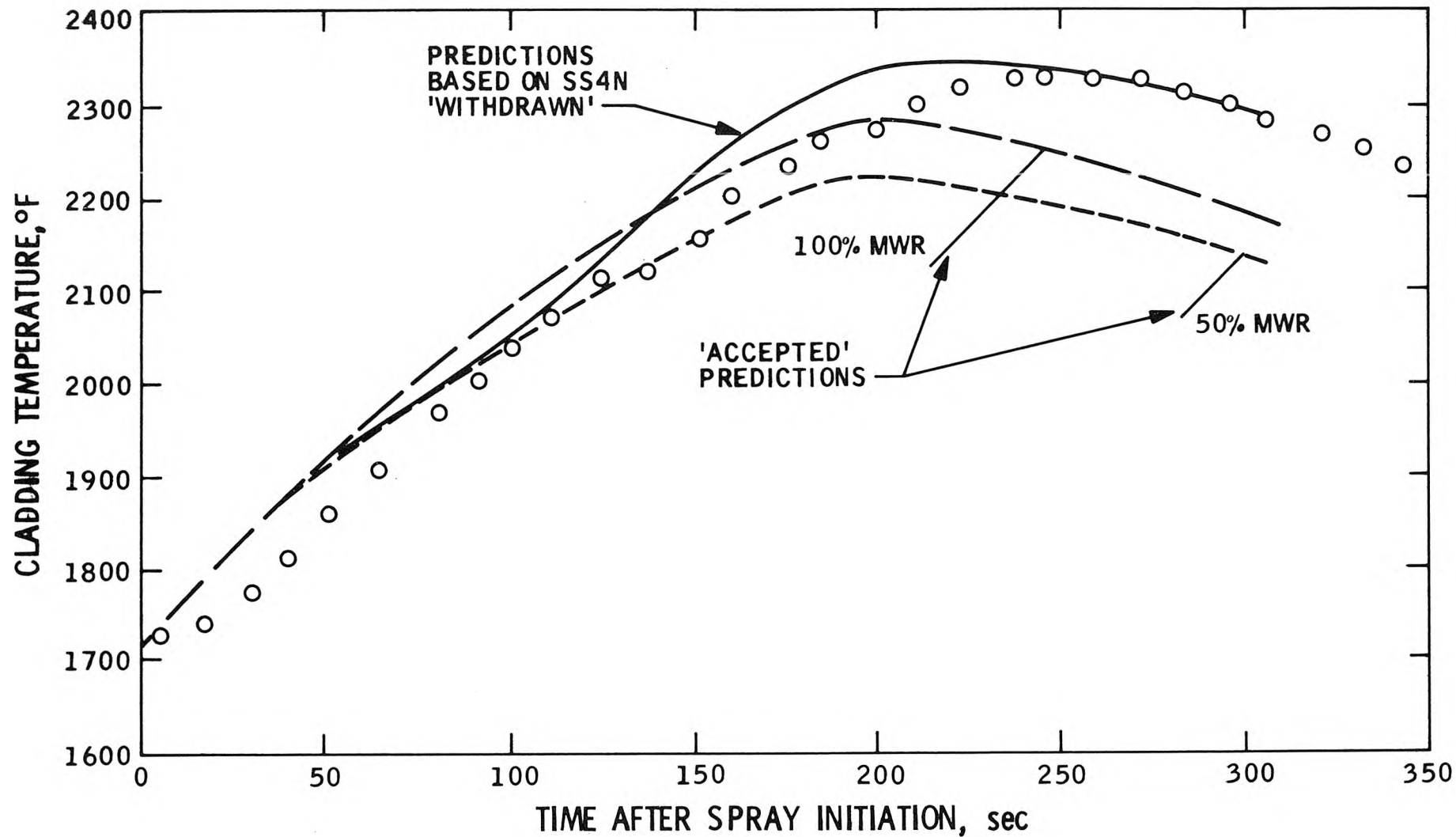
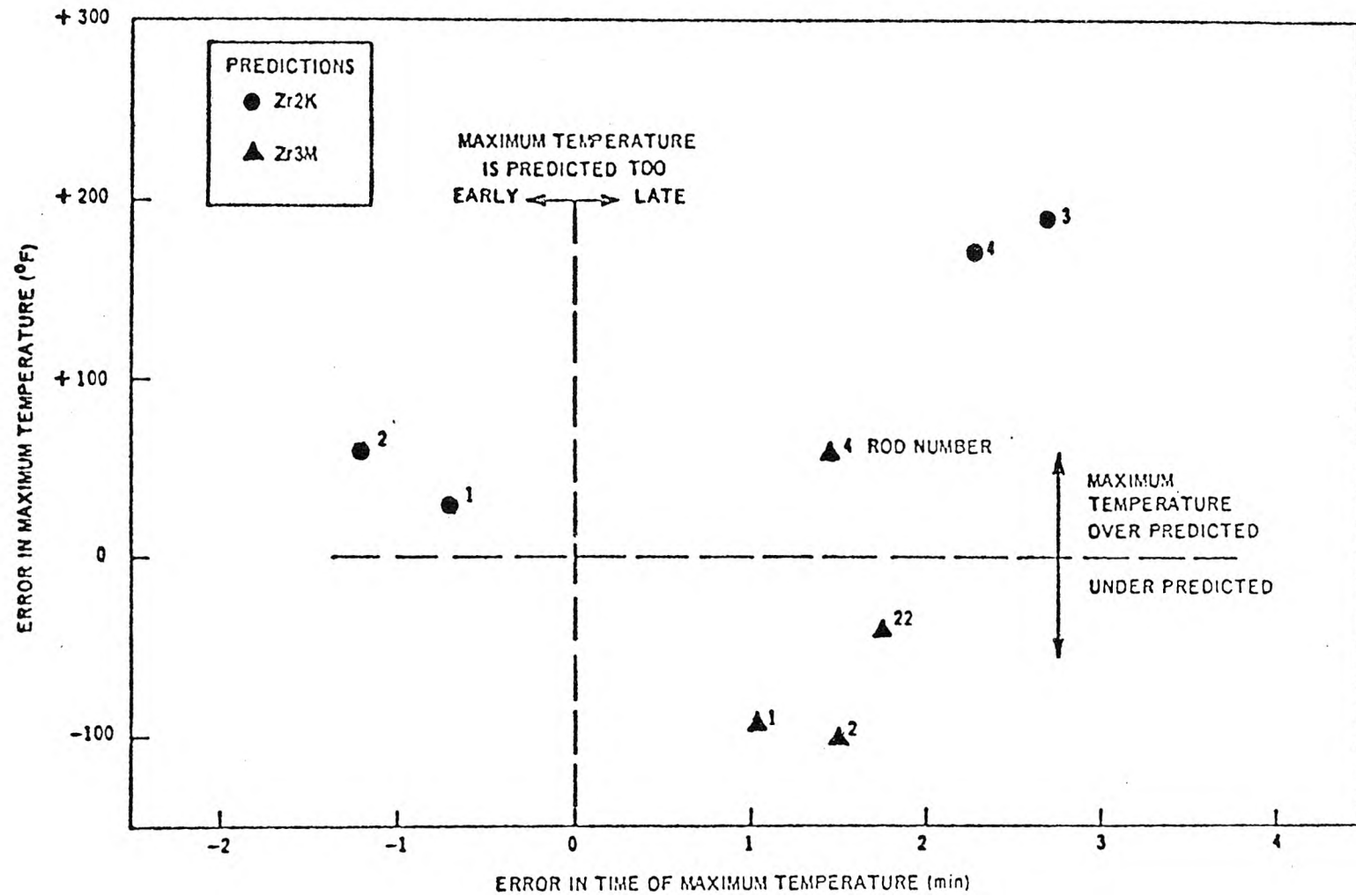


Figure A8.3 CNI Correlation of SS2N and SS4N Results
(After Figure A-25, 52, by permission.)

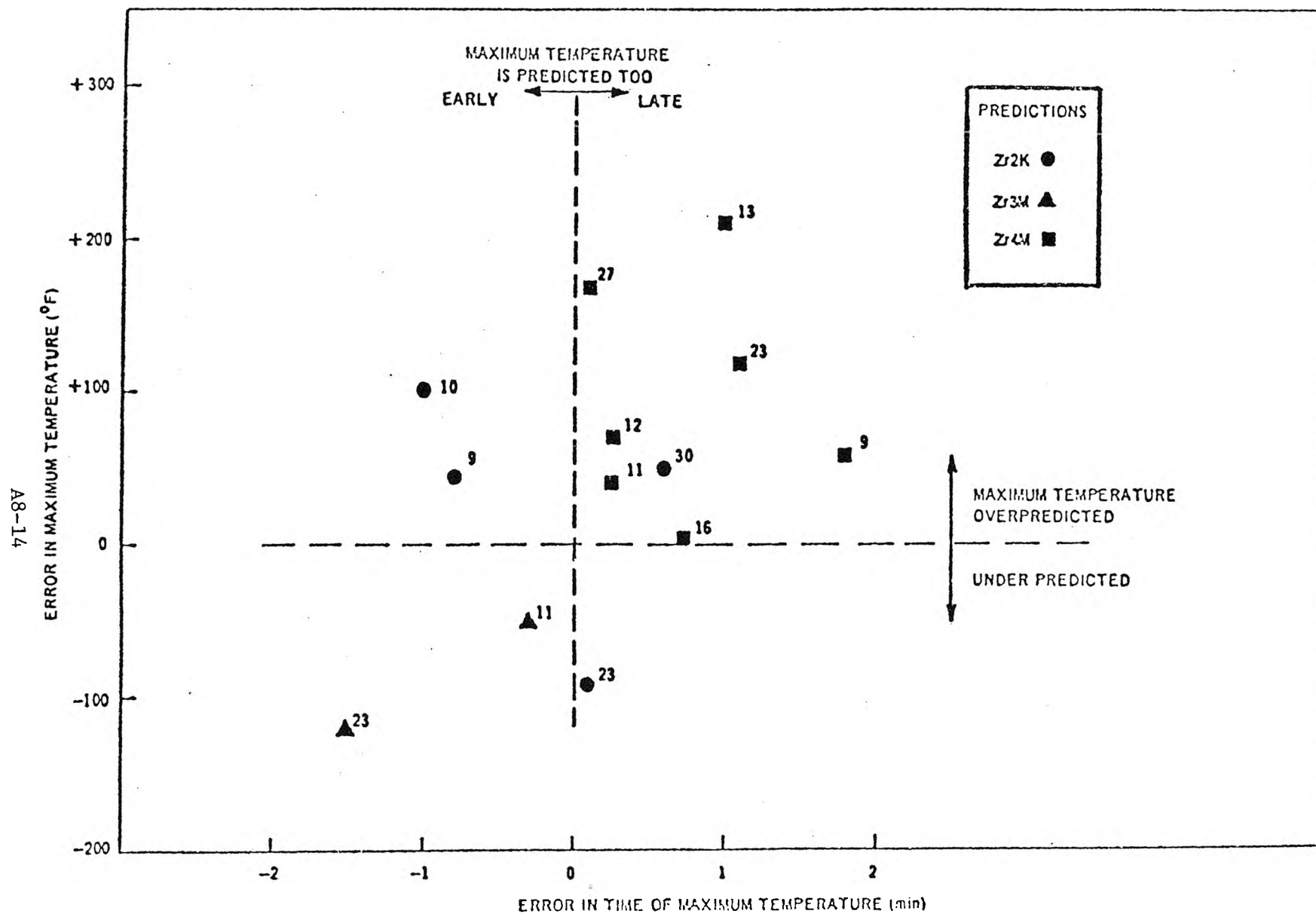
Figure A8.4

Prediction Map for Rod Temperatures



(After Figure A-48, 52.)

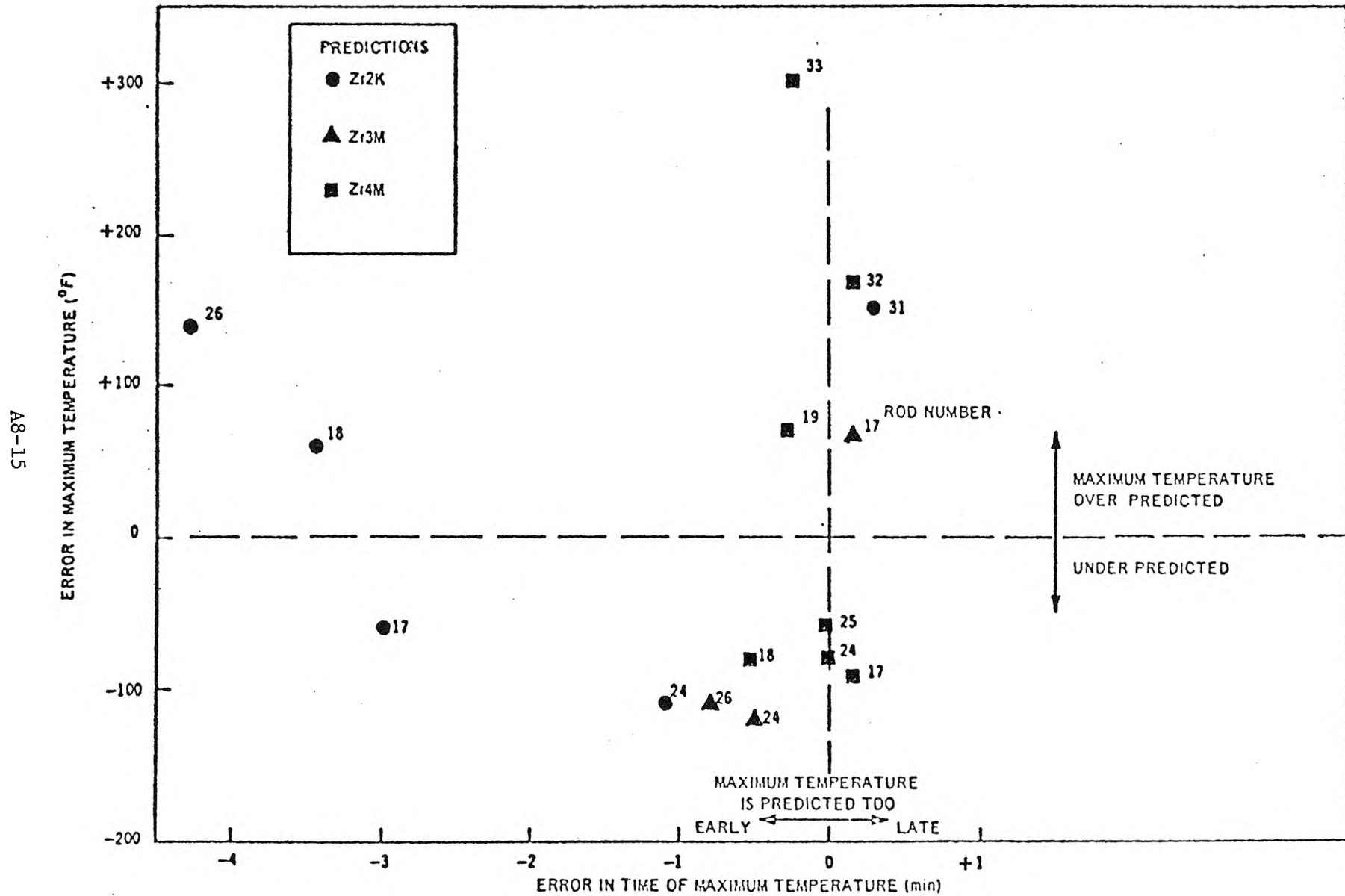
Figure A8.5
Prediction Map for Rod Temperatures



(After Figure A-49, 52, by permission)

Figure A8.6

Prediction Map for Rod Temperatures



(After Figure A-50, 52.)

Peak temperatures calculated on the basis of a 50 percent Baker-Just M-W reaction energy input to the system will tend to be lower than predictions using a 100 percent Baker-Just input (figure A8.3). However, comparisons with the FLECHT measured data, for 50 percent Baker-Just inputs, would tend to accentuate indications of underpredictions, which could make the calculational results tend to appear unconservative. This aspect should be considered when the reader analyzes the results shown in figures A8.4 to A8.6. The curves show the differences in maximum predicted and measured temperatures as a function of the error in prediction of the time of the recorded temperature maxima. It should be noted that although there is a substantial amount of scatter in plotted data, predicted temperatures are generally in excess of measured values using SS2N derived heat transfer coefficients, even using a 50 percent M-W reactions estimate. The disturbing aspects of the results shown are the apparent randomness of the results and the wide limits of the errors in the data. ($-4 < \Delta_{\text{time}} < +3$ minutes; $-150^{\circ}\text{F} < \Delta_{\text{temp}} < +300^{\circ}\text{F}$). The scatter in the results does not tend to encourage great confidence in the reliability of the calculational capability for the design methods. It does, on the other hand, support GE's statement that the "mechanisms of spray cooling are somewhat random" (26) and provide some basis for the CNI concern over non-reproducibility of the tests. The implied uncertainty of +15 percent/-7 percent in predicted temperature maxima seems unpleasantly large. (Note however that the maximum non-conservative difference shown in the above GE figures is the -7 percent). However, the range of errors in predicting maximum temperatures lends support to the concept that maximizing the credibility for the heat transfer methodology would require use of a greater margin of safety in estimating heat transfer coefficients for design purposes.

The results shown (described as having been derived with the current GE design model) (52, appendix A) indicate the application of common design practices but show only uncertain conservatism.

However, note that if a 100 percent Baker-Just M-W reaction is used in the calculation, in accordance with stated IAC requirements, predicted temperature maxima exceed measured values more than 85 percent of the time. A discouraging aspect of the results shown in figures A8.4 to A8.6 is that in one test, Zr3M, temperatures were overpredicted for less than 30 percent of the rods. Even using a 100 percent Baker-Just M-W reaction energy input, overpredictions were achieved on less than 50 percent of the measurements. Predictions of time histories were also poor.

It is also discomfoting to note that central rod prediction, figure A8.6, had the poorest record for overprediction of all the test results. Fifty percent of the results were underpredicted for these rods, which are the hottest in the bundle. Thus the temperatures of the hottest rods were most regularly underpredicted -- an observation which weakens confidence in the conservatism of the analysis methods even more.

A8.3 Zr Test Review

The largest portion of the CNI direct testimony (9) was particularly directed at discrediting the FLECHT Zr2K test (a zircaloy bundle with internally pressurized rods). The Zr2K test results are very important to both the AEC and the CNI, for opposite reasons. The AEC uses the test results extensively to defend the validity of their recommended BWR analysis methods. The CNI try to show the inadequacy of the test in order to weaken the AEC defense.

Fortunately, the Zr2K utilized constant resistance (Kanthal) heater elements which minimized power shifting (52, p. 12). Unfortunately, 10 of the 49 heaters failed, losing power before the test was concluded. Fortunately, all the rods with failures were instrumented with thermocouples and ammeters. Unfortunately, the individual rod circuits were fused for 40 amps while the ammeters were designed for maximum currents of 25 amps and were "pegged" for varying lengths of time on all "failed" rods. Also unfortunately, no ammeter time histories

are shown for the test (perhaps because estimates of the resistance of rod current paths during "failure" periods are highly speculative and hence power estimates based upon current measurement would be equally uncertain).

Fortunately, the Zr2K rods were internally pressurized to simulate the results of swelling and rupture produced by fission product gases in an operating reactor. Unfortunately, the heater failures complicated the responses of the rods. Maximum expansion, flow blockage, and temperatures occurred in the vicinity of the "failed" rods. GE reported 60 percent blockage for the central 9 rod portion of the bundle only. CNI estimates that blockage of 90-94 percent occurred in the off-center portion of the bundle containing the "failed" rods. The CNI implied that the extensive off-center swelling occurred in spite of heater failures (which were inferred by the CNI to be unpowered sinks for thermal energy from neighboring rods). Examination of the rod thermal histories indicates it is more likely that the swelling was induced because of excessive power supplied to the rods during their "failure" periods, producing local hot spots with consequent synergistic expansion and rupture of adjacent rods.

The CNI claimed that the test showed that near "thermal runaway" conditions resulted from M-W reactions, in spite of the "failed" heater rods. They compared test results for SS2N with Zr2K, showing satisfactory correlation during approximately the first five minutes of the test with substantial deviations (Zr2K temperatures greater than SS2N) during the subsequent periods of substantial heater failures. Attempts by GE to show that M-W reactions were insignificant in the thermal response of the rods were not overly convincing since they did not evaluate actual dynamic heat rate inputs but depended instead upon arbitrarily time averaged heat inputs over arbitrary time intervals (54, appendix A). Gross estimates were made of the total energy contributed to the thermal transient through the M-W reaction of 1/4 B/inch of

cladding length (based upon the maximum observed depth of ZrO_2 penetration for the Zr2K experiment of 1.8 mils). This was compared with a design total delivered decay power to the center of the maximum peaked rod over the 24 minute spray cooling transient of 29.7 B/inch (14.5 B/inch over the first 10 minutes). Thus, GE inferred the total M-W reaction to be 5-10 percent of the decay energy depending upon which of the two time periods was used in the estimation. They acknowledge that the rate of M-W energy addition is more significant than the comparisons with total energy shown above, but state that rate information cannot be obtained from the Zr2K data. Irrespective of the validity of this observation, it seems that comparisons with rod input energy increments taken over 10 to 24 minute intervals are too insensitive to be adequate indications of the significance of the M-W energy contribution. No feeling of confidence is gained that M-W reactions were unimportant as a result of this GE analysis. However, the case for M-W induced thermal runaway in the Zr2K test is equally weak.

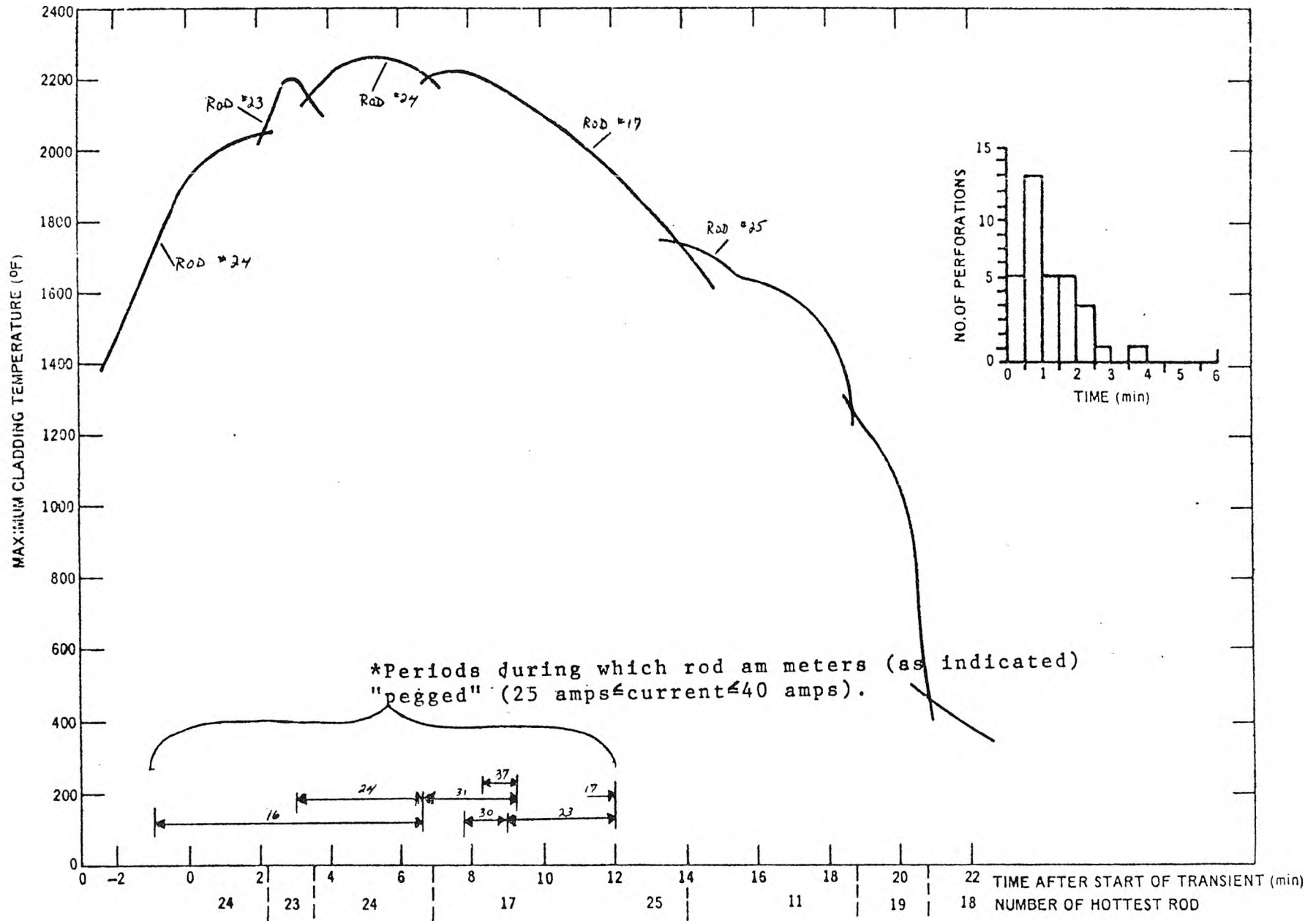
One of the more difficult aspects of evaluation of Zr2K test results is associated with the fundamental data for the tests, the recorded thermocouple (TC) responses. GE has been very liberal with their accreditation of observed TC responses as erratic. However, several proffered examples of erratic response seem to show well defined inter-rod correlations. Under such circumstances, "unexplained" might be a better description for the observed TC behavior than "erratic."

Figure A8.7 (based on material from reference 54) presents an envelope of the thermocouple response histories for the rods which experienced the peak temperatures during the Zr2K thermal excursion. It is interesting to observe the correlation between the rods with maximum temperatures, their periods of maximum temperature, and their relationship to rods in which the currents were "pegged" at levels between 25 amps (the maximum range of the ammeter) and 40 amps (the fused current limit for each rod), and the periods of excessive current prior to rod

Figure A8.7

Envelope of Thermocouple Response Histories for Peak Temperature Rods of FLECHT Zr2K Test

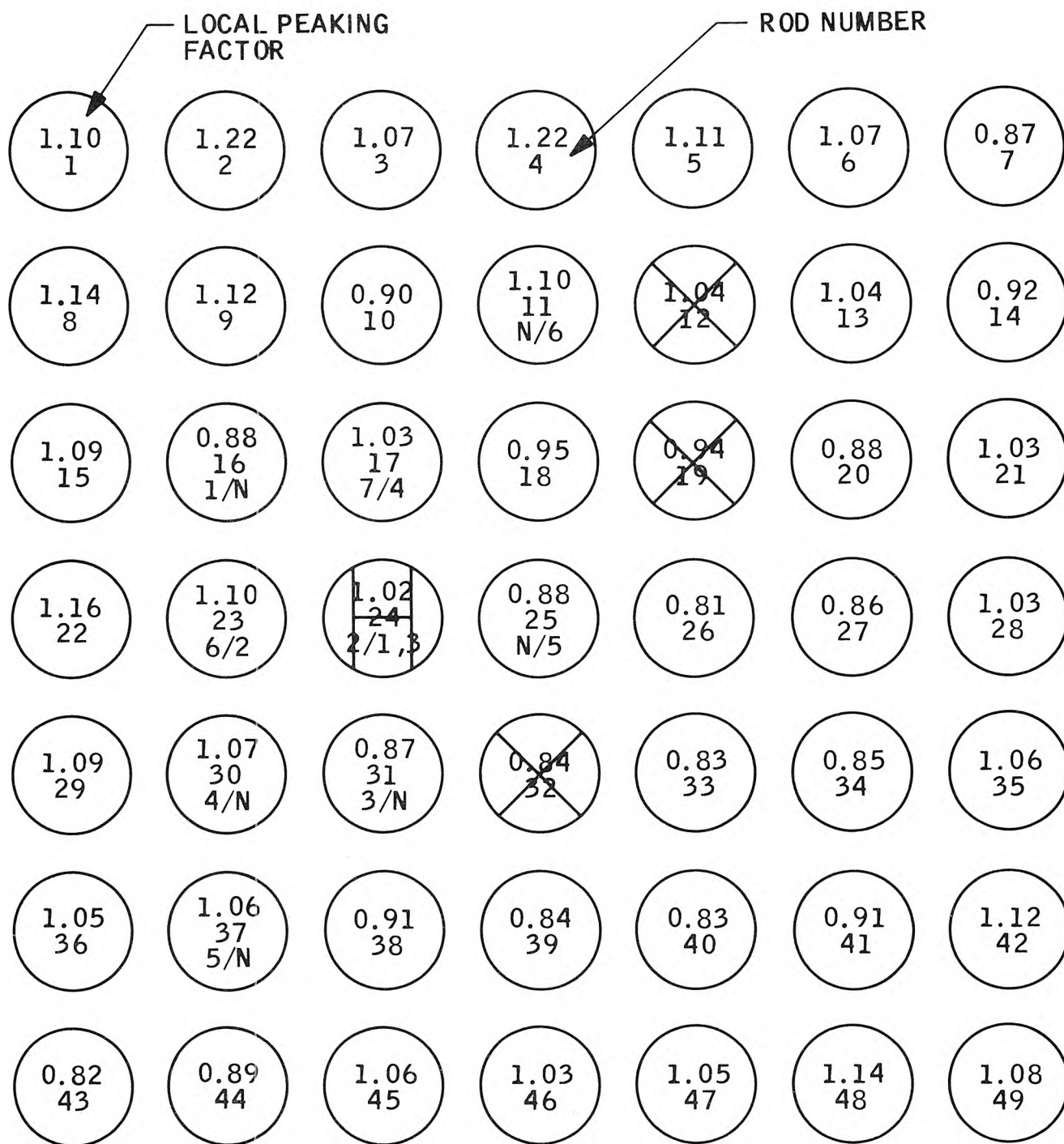
A8-20



(After Figure 8, 54, by permission.)

"failure." A schematic diagram of the rod bundle layout is shown in figure A8.8. Using figures A8.7 and A8.8, it is interesting to compare the order and location of electrical "failures" and the development of peak temperatures.

A rigorously thorough analysis of the Zr2K thermal response measurements is beyond the scope of this report. It should be noted, however, that the recorded temperatures of rod 16, which developed the first electrical anomaly after the official start of the test, were almost identical to those of rod 24, which was given credit for the maximum temperature measurement. The intra- and inter-rod temperature measurements for rod 16 and its neighbors show consistent correlations over the first two minutes of the transient, in spite of the current anomaly being experienced by the rod (which started essentially at the beginning of the thermal transient test period and lasted for nearly six minutes). Between 2 and 3 minutes after transient initiation, however, thermocouples (TC) on rod 16 indicate an apparent sharp temperature rise. Because of the anomalous electrical activity of rod 16 at this time, experimental analysts have been inclined to discount this TC response as anomalous also. However, it is interesting to note that the extreme temperature excursion shown in figure A8.7 for rod 23 (adjacent to rod 16) occurred at the same time the rod 16 TC excursion occurred and is matched by nearly identical temperature excursion in rod 9, the other rod diametrically adjacent to rod 16. Moreover, it seems entirely too coincidental that temperature turnaround should be achieved in rod 24 at essentially the same time that the actual failure (rod current going to zero) for both rods 16 and 24 occurred. Under those circumstances, it does not seem surprising that rod 17, still being driven by "normal" electric current and in direct view of the three hottest rods in the test (rods 16, 23, and 24) should then become the highest temperature rod for most of remaining significant portion of the temperature transient. During this period, rods 17 and 23 both underwent electrical



LEGEND:

 RODS 12, 19, 32 FAILED (NO CURRENT) BEFORE TRANSIENT INITIATION (ZERO TIME FOR TEST).

 ROD 24 HAD HIGHEST PEAK TEMPERATURE MEASUREMENT FOR TEST.

i, j ORDER OF: POST TRANSIENT "FAILURE"/BECOMING MAX TEMPERATURE ROD FOR TEST.

N NOT APPLICABLE.

Figure A8.8 Zr2K Rod Bundle Schematic Showing Rod Failure Sequence
(After Figure 6, 54, by permission.)

anomalies in which excessive currents were delivered to them. It was not until the current to both of these rods actually went to zero, approximately 12 minutes after the thermal transient began, that rod 17 relinquished its role as the highest temperature rod for the test.

The relationships described above seem to indicate a systematic correlation between the electrical anomalies of the "failed" rods and temperature extremes for the bundle. It would appear that a convincing argument could be made that the driving functions for the highest temperature rods was probably excessive power anomalously delivered to the rods during these periods rather than any of the normal physical processes contributing to rod heat up for a reactor bundle under LOCA conditions. Accordingly, it is regrettable that the test report failed to provide any significant evaluation of the relationship of electrical anomalies to measured maximum temperatures, except to imply that the "failures" resulted in lower temperatures for the test than would have been experienced had the "failures" not occurred. The report states:

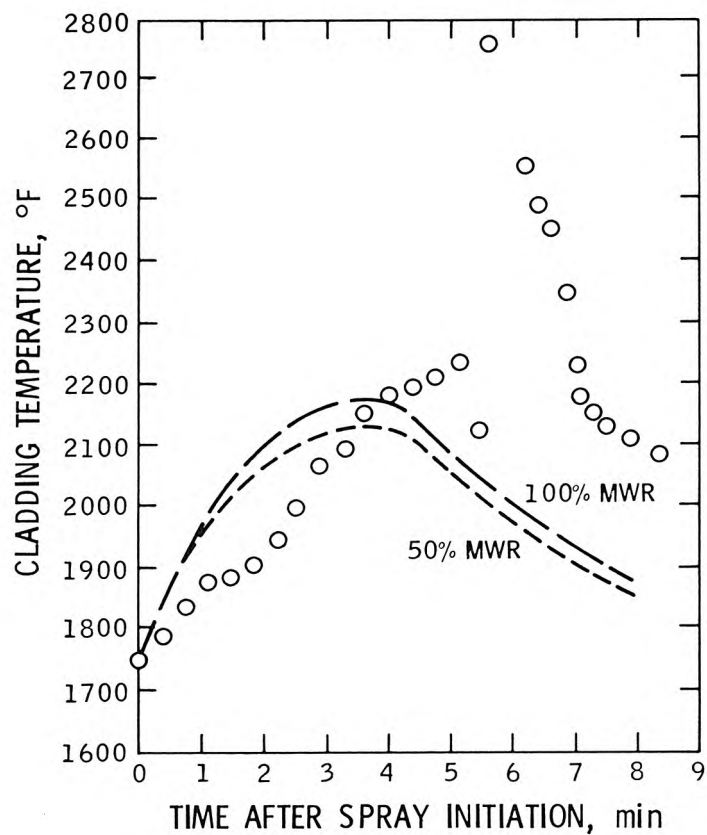
The effect of rod failures later in the transient (e.g., rods 16, 24, and 30 between transient initiation and maximum temperature and rods 17, 23, 31, and 37 after maximum temperature) was not considered in the detail described above. The effect of these rod failures must be small, since they were at relatively high temperatures at the time of failure, and less radiation from the powered rods to the later failing rods can be expected. In addition, the rods which failed after the bundle maximum temperature had occurred could not have affected that maximum temperature. Consequently, the effect of the later seven rod failures is estimated to have had a smaller effect on the bundle maximum temperature than did the first three failures; that is, less than 30°F.

It is therefore estimated that, had no heater rod electrical failure occurred, the maximum recorded cladding temperature would have been no more than 60°F higher than the 2250°F actually recorded (54, p. 67).

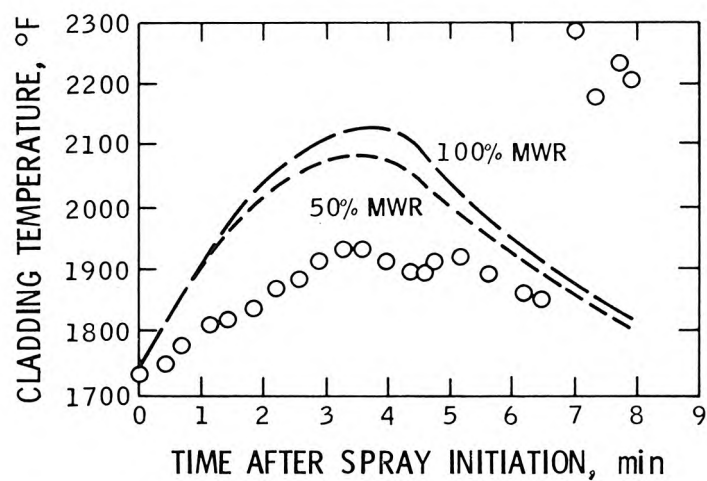
The possibility that electrical anomalies may have acted as high temperature sources rather than energy sinks does not appear to have been considered.

Is there a possible relationship between the anomalous TC readings and M-W reactions? Figure A8.9 is representative of the correlation which GE has made between calculated and measured thermal response for some of the Zr2K rods where TC anomalies have been recorded. CNI has implied that the test was on the verge of "thermal runaway" and was saved only as a "consequence of the extensive heater failures that occurred" (9, p. 5.63). GE authors naturally downgrade this possibility. From the limited data submitted in the test reports, it is difficult to draw any more satisfying explanations for the erratic TC behavior than those given by GE investigators. It is significant, however, that most of the so-called erratic behavior occurs during the periods of heater failures. Figure A8.10, taken from the Zr2K final report, shows the correlation between the rod 24 (the absolute highest temperature rod for the test) electrical anomaly and its TC response (54). A similar relationship could be shown for rod 31 of figure A9.9. Based upon examples such as figure A8.10, the proffered GE explanation that heater short circuits to the rod surfaces could have contributed to some of the unusual TC responses seems acceptable. It is unfortunate that no records of individual rod electrical current or voltage measurements were shown in any of the GE test reports to permit independent evaluation of this source of variance (the individual rod power histories) with the TC responses.

Some of the "erratic" TC readings even GE cannot explain through correlation with electrical short circuits of the heater elements. They note that the majority of these unexplained changes were in the direction of decreasing temperatures or at such low initial temperatures (1800°F to 1900°F) that association of the TC irregularities with M-W reactions is discounted. The GE conclusion is that the "erratic



Bundle Zr2K Rod 24 Midplane Thermal Response Prediction

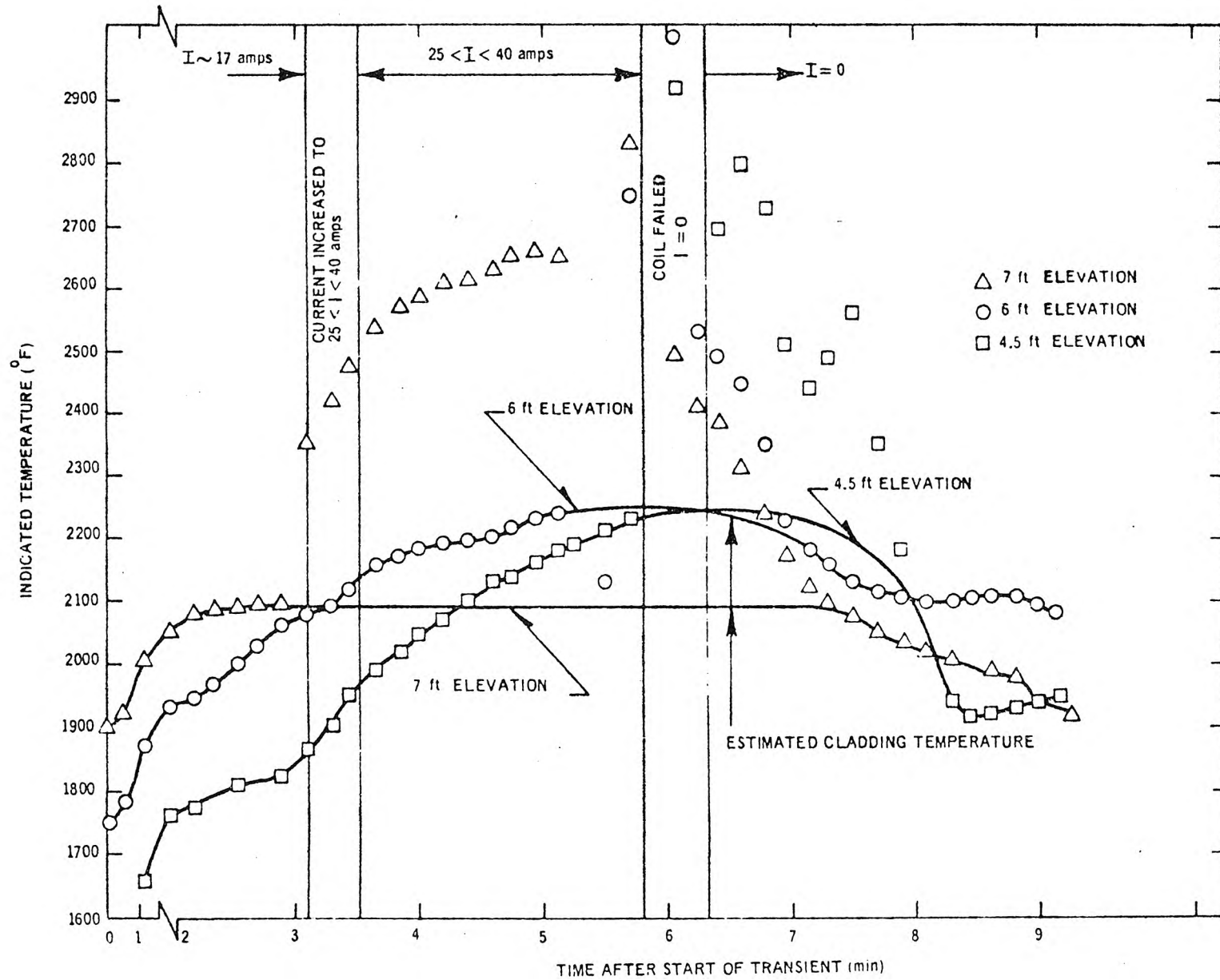


Bundle Zr2K Rod 31 Midplane Thermal Response Prediction

Figure A8.9 Comparison of Predicted and Measured Thermal Histories for Zr2K Rods with TC Anomalies
(After Figures A-11 and A-12 from 52 by permission.)

Figure A8.10

Analysis of Zr2K Thermal Response



(After Figure 12, 54, by permission.)

thermocouple outputs do not represent actual cladding temperatures, but are the result of equipment malfunctions" associated with the Zr2K test (54, appendix D, p. 107).

Based upon analysis of the material presented, it appears unquestionable that the TC response was badly affected by short circuits and equipment malfunction. The net result is that it is not possible to certify that M-W reactions were insignificant in the measured thermal transient, but the case for near "thermal runaway" proposed by the CNI is also unconvincing. It is probable that most of the dramatic TC slope changes, as well as several of the other RC aberrations associated with the test, were short-circuit induced rather than M-W reactions. However, more results seem to be systematically correlatable between rods that the GE test analysis is willing to concede. This leads to uncertainty over the proper interpretation of results. A more thorough analysis and interpretation of the Zr2K-TC data would have been desirable.

The CNI have also observed that the GE analyses regularly predicted that thermal turnaround (the beginning of the temperature reduction for the transient) would occur sooner than was actually experienced in Zr2K. The CNI claim that the retardation of turnaround was caused by flow diversion from the local hot spots to cooler locations in the bundle induced by locally increased flow resistance from smaller rods. GE on the other hand sees the earlier prediction of peak temperatures, with usually higher predicted peaks, as a conservative design tool.

It seems probable that the difference between test and theory results from rigid adherence by GE to a time-dependent model of heat transfer coefficients which were derived from their SS2N tests and adopted as their "design model" (52, p. 26). The design analysis method, based on the SS2N time history, apparently did not permit accommodation of the idiosyncrasies of the Zr2K test experience with its rod heater failures and TC equipment malfunctions. Consequently, the predicted

results might not reasonably be expected to correspond well with the reality of the Zr2K test. Whether or not design basis prediction of LOCA thermal histories would agree well with an actual transient also remains to be shown. Results imply that the GE thermal analysis method may be a weak predictive tool and more effort appears to be needed in model development. However, it does appear that with sufficient analysis, FLECHT results would be adequate to form a basis for demonstrating the development of conservative analytical design methods.

A8.4 PWR-FLECHT

The CNI criticism of the PWR-FLECHT program was relatively mild when compared with the challenge to the BWR-FLECHT test series results. Their conclusion that the tests established that reflood in a PWR-ECCS is at best marginally capable of controlling a LOCA is substantially less severe than the attack on the BWR. The motivation behind this difference in critical intensity is not immediately apparent, but perhaps it may be the result of a general feeling on the part of the CNI that the PWR-ECCS had more obvious problems than the BWR and was consequently more vulnerable to attack at other points. Therefore, it may not have been felt necessary to challenge PWR-FLECHT results as severely as the BWR-FLECHT program.

Criticisms were made by the CNI concerning a number of problems. The experimental design was faulted (especially the use of SS rods in 84 of the 88 tests vs Zr rods in only 4 of the 88). The range of test parameters investigated was criticized as being too limited. The principal objections concerned limiting the test initial temperatures to 2300°F or less and including reflood rates of less than 1 inch/second which are below design practice. Both objections are basically invalid. The 2300°F upper temperature limit is understandable, although perhaps a poor choice for a limit, because it does represent the upper temperature limit of the criteria. The objection to the investigation

of low reflood rates is hard to understand. The low rates tested represent extreme cases for LOCAs, since limiting values of low flow rates for termination of temperature excursions were observed, (i.e., temperature turnaround apparently could not be achieved at flow rates less than 0.8 in/sec for reflood initial temperatures of 1600°F or greater). The results are useful for establishing the conservatism of design values of reflood rates. If, as CNI claims, current PWR reactors are designed for reflood rates of around one inch per second, then the PWR-FLECHT tests show that PWR-ECCS designs may be uncomfortably close to having no margin for error (appendix 9).

The heat transfer coefficients derived from the tests were also challenged on the grounds that no energy balance was performed (or perhaps could be), that local saturation temperature for the convection steam was assumed as a boundary condition, and that the "cold" boundary walls of the test configuration contributed a radiation heat transfer sink which was neglected and would not be present in the open lattice construction of a typical PWR. The issue of radiation to the housing was subsequently reviewed by ANC and found to contribute no more than a ± 5 percent uncertainty to the data (4, p. 17.3).

Perhaps the most valid of all the CNI objections to PWR-FLECHT concerned the alleged failure to adequately investigate the consequences of rod swelling with resulting blockage of core sections. This type of potential flow blockage was simulated in the tests by introducing perforated planar steel orifice plates at the midplane of the bundle so that the resulting fluid flow in the bundle, though reduced below normal delivery rates, was still constrained to be one dimensional. The orifice plates apparently caused the fluid to become more finely divided (and perhaps better distributed) behind the plates, resulting in unsuspected improvements (increases) in heat transfer rates in the immediate vicinity of the plates. As a result of this test, W has claimed that swelling

and rupture may improve heat transfer, a point which seems highly debatable when the one dimensional limitations of the test are considered. The most discomfoting aspect of this problem is that it appears that PWR computational methods may have included this "improved" heat transfer mechanism in their design procedures (8, p. 3-50 and 6, p. 198). This practice, if true, would have been totally at odds with any reasonable interpretation of conservatism. The Commission in their discussion of the new AC appear to have recognized this problem. They summarized the AEC position on the influence of blockage on reflood heat transfer, as follows:

The FLECHT tests simulated flow blockage in a number of runs by the insertion of perforated horizontal plates. With reflood rates of one inch per second or higher, improvement was found in the rate of heat transfer as far as two feet upstream and four feet downstream of the blockage. The improved heat transfer was shown to be caused by break-up of the entrained droplets and increased turbulence (Exhibit 1006a). The blockage in these tests ranged up to complete blockage over several channels with 75% blockage in other channels. For the flow blockage tests at a reflood rate of 0.6 inches per second, heat transfer was degraded by blockage. Presumably the poor results at the low reflood rate were the result of a lack of entrained water droplets, leaving only single phase steam cooling (Exhibit 1113, p. 17-5).

The FLECHT flow blockage tests were criticized on the basis that the flat plates were not typical of bulging of the cladding. However, Davis tried blockage with sleeves versus plates and found little difference.

As a result of these tests it appears that heat transfer coefficients based on undistorted rod geometry would provide a reasonable approach to estimating core temperature behavior during reflood, for reflood rates above one in/sec. For lower reflood rates blockage would have a deleterious effect and one must resort to calculation with single phase steam cooling, taking into consideration the effects of blockage on core flow distribution (60, pp. 1124,1125) (emphasis added).

Thus the potential practice of taking credit for the uncertain benefits of flow blockage in PWR reflood heat transfer is specifically precluded by the new AC.

A8.5 Evaluation of FLECHT Results

In summary, the CNI feel that:

The program [FLECHT] was characterized by narrow scope, limited range of parameters investigated (many inappropriate to the tasks at hand), the use of incorrect materials, crude and incompetent instrumentation and operating techniques (with consequent major equipment malfunctions), and, as a culminating weakness, expansive and overgenerous interpretations [of test results] (7, p. 5.37).

In the AEC's Supplemental and Concluding Testimony, they have attempted to answer most of the CNI objections (7, chapter 5) on a point by point basis (4, chapters 16 and 17; 6, pp. 163-177, 194-198). Considering the test program from the standpoint of bundle design materials and test conditions, they have attempted to address the program scope and range of parameters for all parameters investigated. The AEC review of the engineering basis for selection of the ranges of parameters tested is reasonably convincing. In only one area, that of peak power delivered to the test bundle, is the range of the program (especially BWR-FLECHT) acknowledged to be weak. The AEC acknowledges that none of the BWR-FLECHT tests were conducted at full reactor bundle power (6, pp. 195, 196). Though the AEC attempted to show that heat transfer coefficients derived from the tests are only weak functions of power, the relatively low powered test results were sufficiently inconclusive that they do not satisfy even the AEC itself. The AEC claimed that the tests were adequate, since the heat transfer coefficients are functions of temperature and the peak temperatures for the test exceeded the criteria limits. They acknowledged, however, that "one or more" zircaloy tests at power levels representative of current design "would reduce the uncertainties in the evaluation model" (4, p. 16-41).

In general, however, they state:

The ultimate usefulness of the BWR-FLECHT test data is not a plotting of peak clad temperatures as a function of test parameters, but is the development of a quantitative description of the basic heat transfer mechanisms operative during spray cooling and flooding (4, p. 16-27).

Though the statement is specifically directed at BWR-FLECHT, its principal conclusion about the ultimate usefulness of the data being not for development of quantitative relationships for peak temperatures against various parameters but for development of general descriptions of heat transfer mechanisms is equally valid for both PWR- and BWR-FLECHT programs.

The AEC has acknowledged that, even in its own labs, there is a divergence of opinion as to whether core heat-up models based on the results of tests with SS rods can predict the thermal response of zircaloy rods "within the accuracy of the experimental measurements." The Supplemental Testimony notes that "ORNL has commented that the poor quality of the test data makes these conclusions uncertain" (4, pp. 16-39). Though it acknowledged that "the quality of the test data is poor," the AEC contends that the poor data affects only "the accuracy with which the core heat-up [model] can predict temperatures and has not prevented cooling mechanisms from being understood and described" (4, p. 16-21). The major uncertainties in modeling the cooling mechanisms are stated to occur in the "values of the convective heat transfer coefficients and the time of channel quench" (4, p. 16-41). It is argued that conservatively low values of convective heat transfer coefficients have been assured by requiring the radiative heat transfer for the SS2N tests (from which the model heat transfer coefficients have been derived) be evaluated at a conservatively large value for the tests. Assuming basic adequacy of the SS2N experimental data, this should lead to underestimations (in the direction of conservatism) of the derived convective heat transfer coefficients. This concept seemed intuitively satisfying. However, to demonstrate how intuition, and poor preliminary analysis, can adversely affect judgement, the new

AC have revised the AEC evaluation of the influence of cladding emissivity and radiative heat transfer. In their discussion of the new AC, the Commission has noted that:

The values of the calculated convective heat transfer coefficients depend to some extent upon the value used for the thermal emissivity of the stainless steel, since the convective heat transfer is obtained after subtracting the radiative heat transfer from the total. Theoretically a high value of the emissivity leads to a low calculated convective heat transfer coefficient. Values of the emissivity measured after the tests ranged from 0.6 to 0.9 (Exhibit 461, p. 81 and Exhibit 1113, p. 16-14), and to add conservatism to the calculation, the Interim Policy Statement required the use of the highest measured emissivity, 0.9, for the calculation of the convective heat transfer coefficients. However it turned out that this resulted in a higher coefficient (less conservative) for the critical inner rods, with a higher estimated standard error. (Exhibit 461, Table 2.) After reviewing the derivation of the coefficients as given in Exhibit 461, we believe that those originally listed as best estimates by General Electric are the most credible and should be used. The effect of this change on the peak cladding temperature will be small, about five degrees according to Exhibit 461 (60, P. 1125) (emphasis added).

The SS2N test was run at a pressure of one atmosphere. In practice, the blowdown of the reactor steam supply system into an intact containment vessel will induce equilibrium pressures greater than one atmosphere within the reactor core during spray cooling or reflood. (A pressure of approximately two atmospheres is expected.) Under these conditions, the SS4N test results (figure A8.11) indicate a general improvement would be expected in heat transfer characteristics with increasing pressure. These factors support the contention that convective heat transfer coefficients (based on SS2N results) should be conservatively modeled.

To help ensure conservatism in channel quench times for the evaluation model, the criterion require that calculated quench times

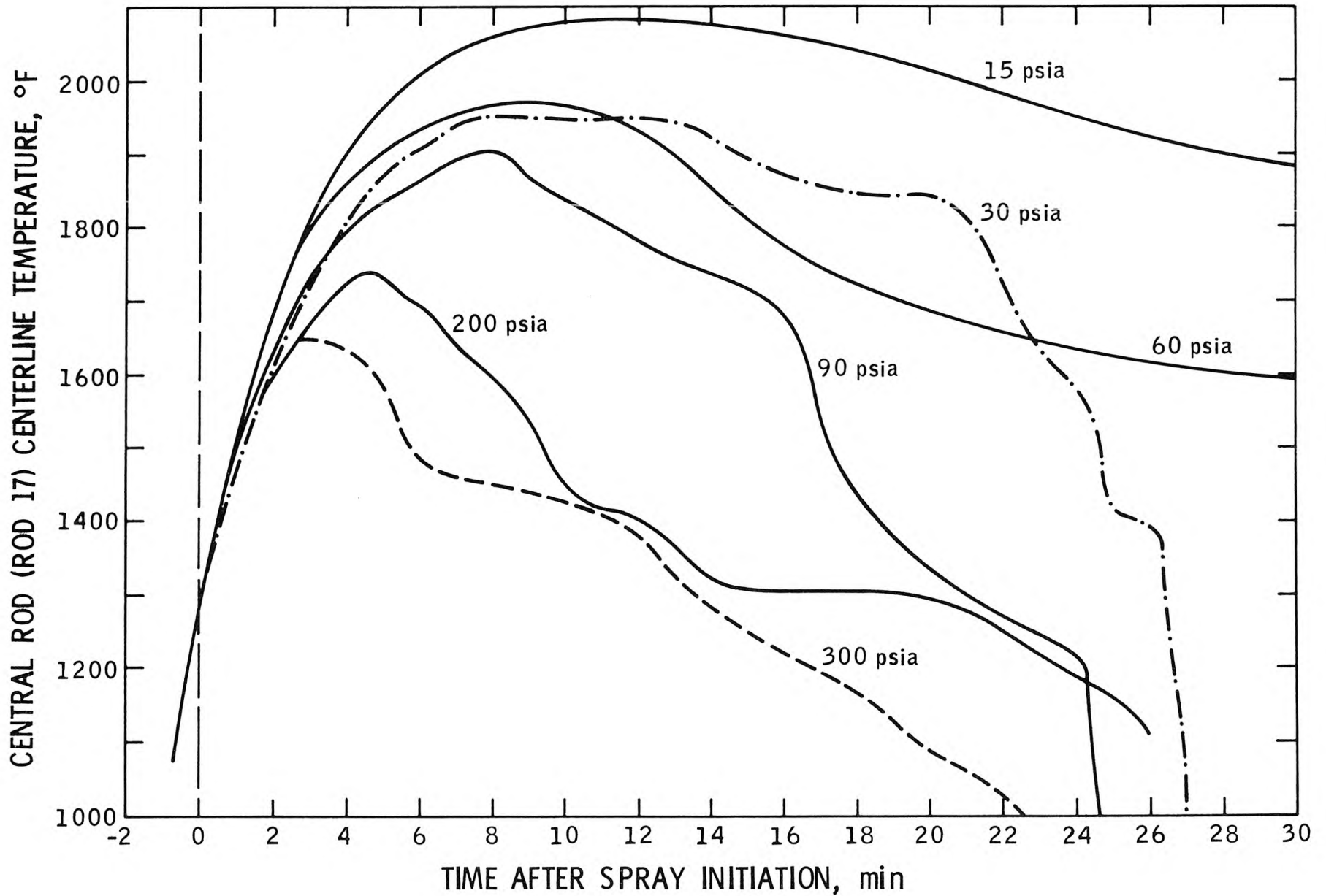


Figure A8.11 BWR-FLECHT (SS4N) Test Results for Evaluation of Effects of System Pressure
(After Figure 6, 52, by permission.)

for BWR channel box walls be augmented by an additional 60 seconds in applying the model to calculate results for a LOCA.

As a general conclusion, it appears that the FLECHT tests have had many problems and weaknesses. Critical zircaloy tests, such as Zr2K for BWR-FLECHT and 9573 for PWR-FLECHT have been marred by problems with malfunctioning test equipment, indeterminate and poorly evaluated data, and inadequacies in the analysis of results. According to the CNI, the "demonstrated defects" in the BWR-FLECHT program were so extensive that virtually no credence could be put in them. It must be acknowledged that the shortcomings of the test program are sufficiently numerous that they provide a potential source of almost unending controversy in evaluating the test results. The ORNL comment, previously cited, that the quality of the tests was poor and conclusions relative to the adequacy of core heating models derived from SS test results are uncertain appears to be understandable, if somewhat extreme, when compared with the reported results. However, the broader conclusions of CNI that the tests are totally inadequate to support development of conservative core spray/reflood heat transfer analysis methods seem too extreme. The tests do demonstrate that the existing coolant application methods are capable of inducing temperature turnaround under adverse conditions, substantially in excess of criteria temperature limits. Even when temperatures exceeded criteria limits and energy may have been delivered to rod bundles at rates in excess of DBA design conditions, the core spray and reflood mechanisms were adequate to achieve temperature turnaround. Furthermore, little substantial evidence was shown for "runaway" energy input to the fuel rods from M-W reaction in excess of the capability of the coolant modes -- as long as criteria temperature limits to the thermal excursions were reasonably well maintained.

However, as indicated in figures A8.4 to A8.6, the current BWR numerical analysis method did not provide completely conservative

analyses of the temperature extremes for the zircaloy tests. Although predicted peaks were generally greater than measured values, they were not consistently higher. In fact, on the basis of evaluations using 100 percent Baker-Just M-W reaction rates, approximately 15 percent of the predicted peak temperatures for tests Zr2K, Zr3M and Zr4M were lower than their measured values. This number appears too high for an adequately conservative evaluation model. Moreover, there is substantial uncertainty with respect to estimation of the time of temperature turnaround. Though the weaknesses in calculational time histories may have resulted from too rigid a dependence upon the details of the SS2N heat transfer coefficient-time histories in application to the zircaloy tests, the uncertainty in prediction of peak temperatures and turnaround times for these tests does not create great confidence in the evaluation model's adequacy.

Consequently, though it would appear that conservative core spray and reflood heat transfer analysis methods may be derived from FLECHT test results, the evidence is weak that current models will give completely conservative results. In fact, the Commission's AC opinion states that:

The accuracy of the FLECHT-determined heat transfer coefficients has been examined several times. (Cf, the review in the Babcock and Wilcox Concluding Statement, pp. 202-204.) Westinghouse estimated a possible uncertainty of 12% in the coefficients. (Trans. page 6878.) The Aerojet Nuclear Company concluded "that the FLECHT data currently represent a best estimate of the heat transfer that will occur in a large undistorted core." They also concluded that an allowance of up to 20% may be needed "to bound the data due to experimental and inferential errors." (Exhibit 1113, p. 17-14.) The Commission approves of the use of the FLECHT data for calculating PWR reflood heat transfer, but notes that these will be more nearly "best estimate" calculations than bounding calculations (60, p. 1124) (emphasis added).

Thus the questionable conservatism associated with BWR-FLECHT calculated reflood heat transfer results is acknowledged. Similar difficulties are acknowledged with BWR-FLECHT heat transfer coefficients. The Commission's AC opinion states:

The BWR-FLECHT convective heat transfer coefficients were determined from the residue of a thermal balance after all of the known inputs and outputs were calculated. The factors considered were the electrical heat input, the rate of change of the heat content of the rods as calculated from the temperature history, and the calculated radiation from the rods to each other and to the channel walls. The residue from these inputs and outputs was ascribed to convective heat transfer. The convective heat transfer coefficients so determined could not be very accurate because their calculation involved taking the difference between two large numbers. The coefficients so obtained are small and are about what one would expect from the mechanisms of natural convection and radiation to steam (Exhibit 1113, p. 16-14).

There has been a great deal of criticism of the BWR-FLECHT tests, particularly by the Consolidated National Intervenors (Exhibit 1041, Chapter 5), and both General Electric and Regulatory Staff have defended them (Closing Statements). However, for the purpose of calculating the maximum cladding temperature, only the derived heat transfer coefficients are of any great importance. The values obtained have always been known to have a high statistical error; furthermore the values are low and reasonable, and there seems little to be gained by renewing the controversy over the manner of conducting and interpreting all features of the tests.

The high but inevitable statistical error of the coefficients for the inner rods (1.5 ± 1.0 BTU/hr-ft²-°F) is bothersome and leads to an estimated error band of as much as $\pm 200^\circ\text{F}$ in the calculated peak temperature in some circumstances (Exhibit 1113, p. 16-36). The test bundle SS2N was used to derive the heat transfer coefficients; another test bundle SS4N, resulted in cladding temperatures 200°F higher than those of the bundle used as a standard; one half of this discrepancy could be explained by test differences, with the other half left to be attributed to statistical variations (Exhibit 1113, p. 16-38).

The problem of these large statistical errors in the convective heat transfer coefficients is compensated to some extent by the fact that the coefficients were determined at atmospheric pressure, whereas the reactor would be at some elevated pressure at which the heat transfer would be improved (Exhibit 111, p. 16-26) (60, pp. 1126, 1126) (emphasis added).

A large degree of uncertainty is therefore acknowledged to be associated with the use of FLECHT derived heat transfer parameters in estimating LOCA temperature histories. Some additional work on the analysis of the FLECHT data with respect to the application of the data to the evaluation models would appear to be appropriate in order to achieve greater conservatism. Additionally, as recommended by the AEC, some additional testing of current bundle designs under design power level temperature excursions would be desirable as well as more tests utilizing zircaloy rods and improved blockage simulation. With these additional actions, it should be possible to demonstrate conservatism of the core spray/reflood heat transfer evaluation models sufficiently to satisfy the requirement of "reasonable men."

Appendix 9 OBSERVATIONS ON SELECTIONS FROM THE ACRS LIST OF ITEMS
OF UNPROVEN CONSERVATISM

Detailed evaluation of all of the elements of the ACRS list of items "considered not proven to be conservative" is beyond the scope of this presentation. However, selected observations on some of the more critical elements of the ACRS list, not discussed in previous appendices, will be given below.

A9.1 Analytical Models and Numerical Methods

The AEC Water Reactor Safety Program Argumentation Plan (16), at the time of its submission (November, 1971), was particularly emphatic in its conclusions regarding the adequacy of the reliability of the evaluation models as licensing tools. It stated:

To date, evolution of codes has not kept pace with the development of emergency core cooling systems. As reactor designs and their operating characteristics changed, the analysis methods were 'patched up' rather than redeveloped, with the net result that overall, existing methods are inefficient, inflexible and do not adequately represent the physical phenomena intended (16, p. 8).

In conclusion it noted:

...the codes are unable to describe important physical phenomena and therefore are unable to confidently define safety margins, their treatment of common phenomena is inconsistent, and they overemphasize the use of empirical correlations (16, p. 27) (emphasis added).

Though somewhat dated, this criticism appears to be largely valid when applied to the current description of the state-of-the-art of numerical methods for ECCS analysis.

The CNI has objected to "the failure of GE to have made available sufficient description of their LOCA transient analysis methods to permit a full independent analysis" (7, p. 4-16). Essentially, the sole description of the GE-ECCS evaluation model is provided by NEDO-10329 (53). Though it is possible to determine a large number of the

model's basic features from the document, it is the contention of the CNI that the report does not adequately describe "the necessary justifications for the assumptions and simplifications" in the code (7, p. 4-17). Moreover, the CNI claim that no valid evaluation of the GE code has been made by the AEC since no independent BWR evaluation codes have been derived by ANC or any other of the independent AEC labs.

The AEC, on the other hand, in their initial Direct Testimony indicated that at least a partial independent analysis has been conducted of the GE model. Their testimony stated:

To assist us in our evaluation of the core heatup model we requested the Aerojet Nuclear Company (ANC) to perform an independent analysis of the fuel cladding thermal transient following the LOCA. This analysis was performed using the computer code MOXY. Although the analytical model incorporated into MOXY is similar to the model used by GE, the formulation and development of the ANC code were done independently. As part of the ANC analysis an attempt was made to duplicate the fuel rod cladding temperature response calculated with the GE heatup standard input assumptions for the analysis... As can be seen by [the] results the agreement between the two analytical models is good (8, p. 4-23).

The statement was accompanied by curves showing good agreement between the ANC and GE code elements for a sample problem on which both had been exercised.

While it appears that some evaluation of the GE models may have been initiated, it also seems evident that not as much effort has been put into the development of independent methods for evaluation of BWR codes as has been put into PWR code evaluation by ANC. This is probably due to the intense involvement of ANC in the development of the LOFT test reactor (a small scale multi-loop PWR) at the AEC Idaho test facilities.

To resolve this problem (now, and in the future), the AEC has incorporated a requirement into its revised criteria for documentation

of all vendor evaluation models (60, pp. 1126, 1127). The proposed standards specify requirements on acceptable documentation and set general standards for model acceptability. While the AEC's proposed rule gave detailed specification of vendor models in terms of approved codes and parameters (6, pp. 57-73), the AC gives no specific approval to any elements of the current vendor analysis methods. On the contrary, it calls for demonstrations of the adequacy of vendor computer programs through demonstrations of numerical convergence and performance of sensitivity studies to evaluate the importance of parameters. Comparisons of model calculational results with applicable experimental information, where available, are also required (60, p. 1127). The clear implication of the Commission's changes between the PR and the final AC is that more evaluation of the analytical models is required before they are to be found acceptable. In their discussion of the AC, the Commission noted:

The need for nodding and sensitivity studies for the computer programs is clearly reflected by the hearing record.....

The need for comparisons of the calculations of analytical models with experimental data is discussed and the value is recognized in the written testimony of nearly all of the participants, including the Regulatory Staff.....

In their comments, Babcock and Wilcox suggested omission of the technical review of the evaluation models. It is the Commission's opinion that, with the changes being made by this rule, it is necessary that a technical review of the evaluation models be made by the Commission; this review is the responsibility of the Regulatory Staff (60, p. 1127) (emphasis added).

Perhaps the fundamental concern of all who have dealt with large scale numerical calculational methods was summarized by Alvin Weinberg, then Director of ORNL. In a February 9, 1972 letter to James Schlesinger then AEC Chairman, Weinberg wrote:

With respect to the criteria themselves, I have only one point to make. As an old-timer who grew up in this business before the computing machine dominated it so completely, I have a basic distrust of very elaborate calculations of complex situations, especially where the calculations have not been checked by full-scale experiments. As you know, much of our trust in the ECCS depends on the reliability of complex codes. It seems to me--when the consequences of failure are serious--then the ability of the codes to arrive at a conservative prediction must be verified in experiments of complexity and scale approaching those of the system being calculated. I therefore believe that serious consideration should be given first to cross-checking different codes and then to verifying ECCS computations by experiments on a large scale and, if necessary, on full scale. This is expensive, but there is precedent for such experimentation--for example, in the full scale tests on COMET and nuclear weapons (29).

While it may be true that a great deal of effort has been expended to evaluate elements of the numerical codes against corresponding experimental pieces of the LOCA, the need for code verification against system test of the magnitude and complexity described by Weinberg is very real. Subsystem or component tests are a poor substitute, acceptable only on a temporary basis in place of a large scale ECC system test.

A9.2 Treatment of Break Flows

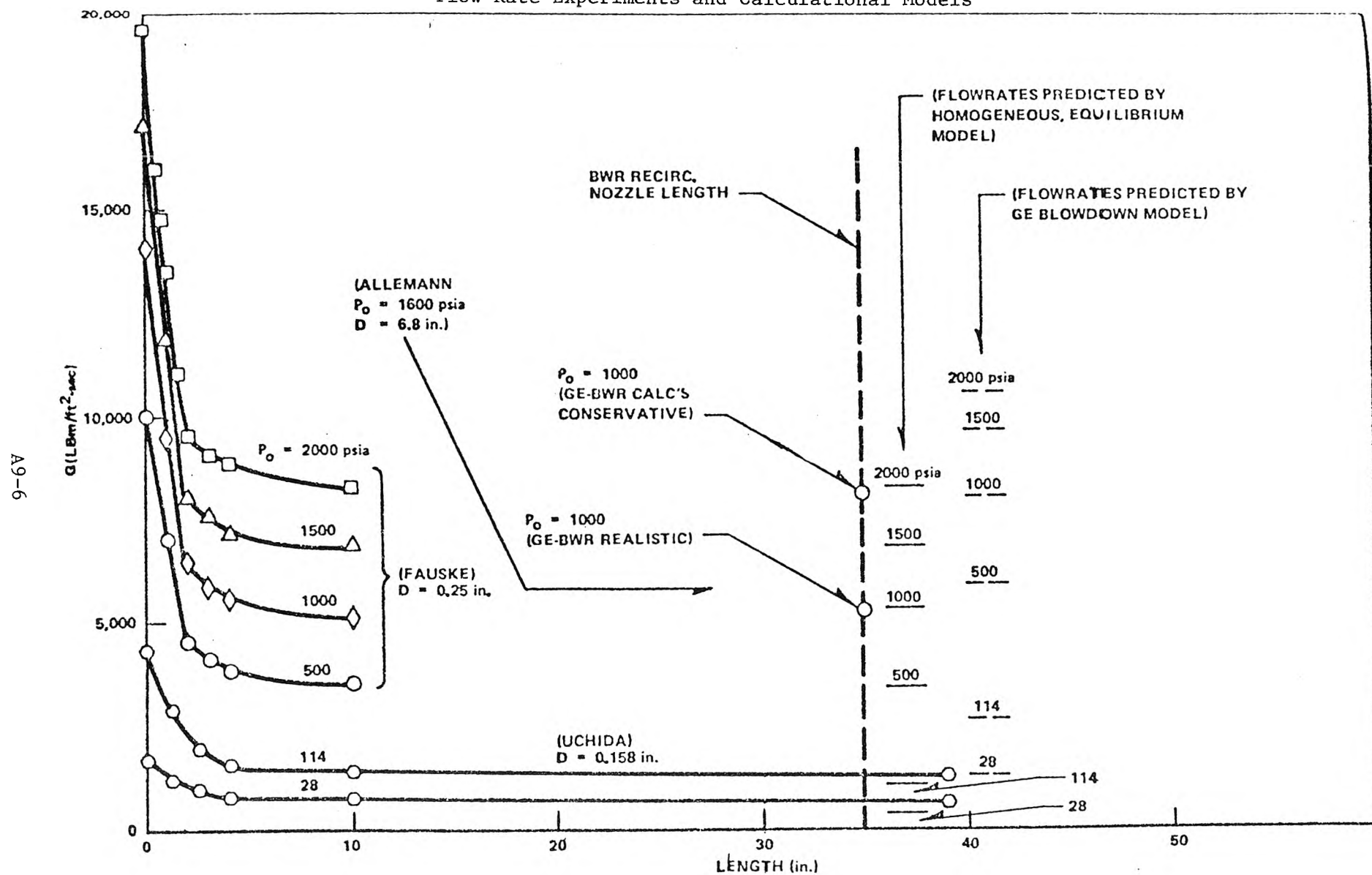
The ACRS contention of lack of proven conservatism for the treatment of break flows of reactor coolant from the DBA double-ended pipe break for both PWRs and BWRs lent support to the CNI claim of "incorrect prediction of blowdown rates" for the reactor. That major variations in the break flow and consequent blowdown rate could potentially seriously affect the thermal history of the reactor is clear. Higher blowdown rates imply shorter blowdown periods which could lead to higher containment pressures and conceivably higher rod temperatures prior to emergency coolant injection.

The AEC has prescribed the use of the Moody fluid discharge model (55) both in their Concluding Statement (6, p. 105) and the final AC (60, p. 1108) as well as in their initial Direct Testimony (8, pp. 2-41 & 4-15) and in the IAC themselves. Moody's analysis method was developed for flow of a two-phase (liquid-steam) mixture through pipes based upon an idealized isentropic equilibrium model of the flow. CNI attempted to show that for relatively short pipes, where the length-to-diameter ratio was short (less than 10), two-phase equilibrium would not exist and metastable liquid flow (flow of pure liquid at temperatures and pressures where vaporization would be expected under equilibrium conditions) would take place (9, chapter 8). Under these conditions, based upon experimental results of Fauske (56), greatly increased break flow rates could be obtained ("1.7 times greater than the rate predicted by the designers for two-phase flow") (9, p. 8.1).

A rather complete summary of the current investigations of blowdown break flow is given in GE's Redirect-Rebuttal Testimony (27, Section I). The GE study, prepared by Moody et al., has a comparison of calculational models and Fauske's experimental work in terms of flow rate as a function of pipe length. Figure A9.1 has been reproduced from it. The argument is presented by GE that although flow rates for very short pipes (less than 2 inches in length and 0.25 in diameter) are substantially higher than rates for long pipes, choked-equilibrium flow is established for pipes longer than 2 to 4 inches. The curves in figure A9.1 are very dramatic in showing the significant difference between the high rates of metastable flow, found in pipes of very short length, and the equilibrium flow rates. The highest metastable flow rates are approximately a factor of 3 greater than the equilibrium-choked flow conditions for relatively long pipes. It is apparently this potential for flow rates truly dramatically different than those predicted for equilibrium flow that caused the concern of the ACRS and CNI.

Figure A9.1

Comparison of Results of Blowdown Break
Flow Rate Experiments and Calculational Models



(After Figure I-1, 27)

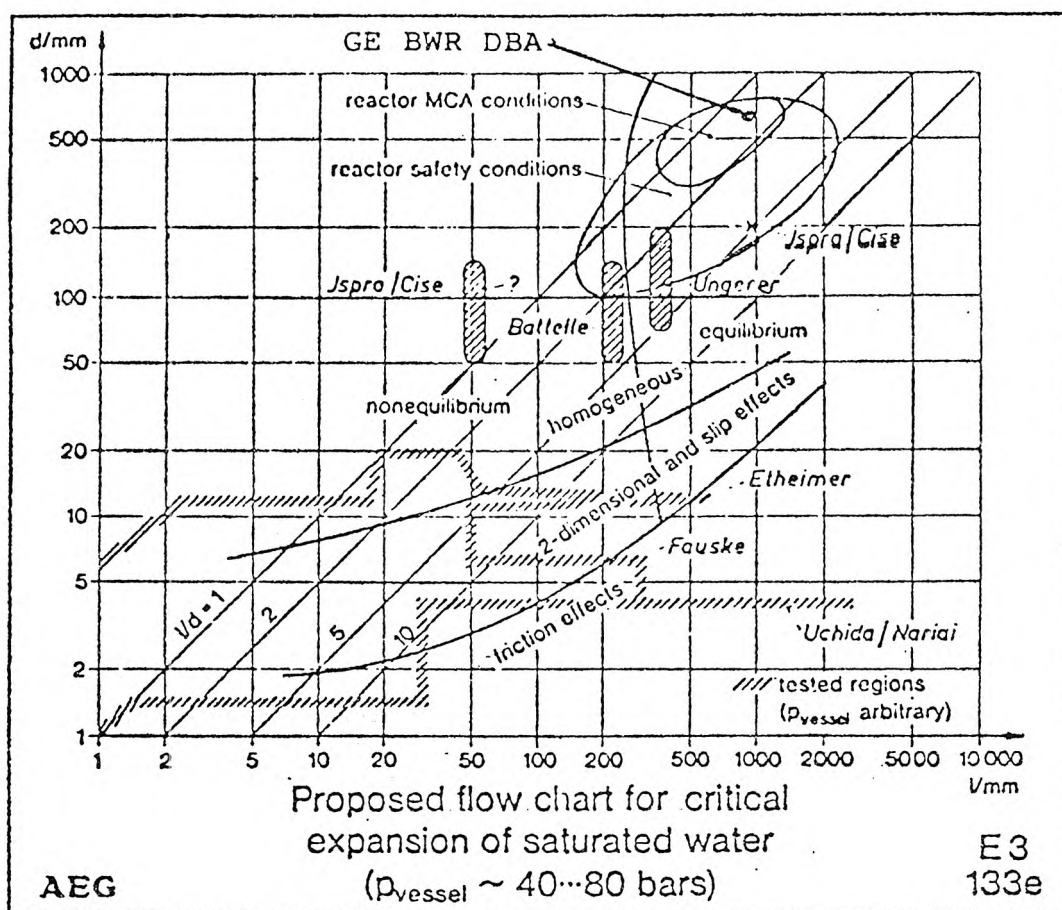
The GE rebuttal argument attempts to show that the length of the pipe is the only controlling factor in the change from metastable pure liquid flow to equilibrium-choked flow. This argument, contrary to Fauske's contention that length-to-diameter ratio is the controlling parameter, is only weakly established by GE. In fact, some of the results presented in the GE report (but not discussed therein) indicate a rather pronounced influence of pipe diameter on the flow transition point in pipe length. A general trend towards increasing lengths for transition flow with increasing pipe diameter can be seen, giving some support to Fauske's and CNI's claims.

GE's stated position is that "the primary determinant of whether the flow is essentially equilibrium two-phase, rather than metastable super-saturated pure liquid, is the length of travel from the pressure vessel." Consequently, the GE report contends, "the design-basis GE recirculation line break, with 35 inch (890 mm) nozzle length and 26 inch (660 mm) diameter, falls in the range accurately predicted by homogeneous equilibrium models" (27). Moreover, it is claimed that the Moody method (a "conservatively" based homogeneous, equilibrium model) will predict larger flow rates (conservatively) from the break than would logically be expected from a "more realistic" homogeneous equilibrium model (see figure A9.1).

The problems with the GE position are two-fold. First, as indicated in figure A9.2, the majority of the testing which has been performed (shown inside the cross-hatched regions) has been done with relatively small diameter pipes, mostly on the order of 4 inches in diameter or less. For these tests, when characteristic length-to-diameter values are constrained within reasonable reactor DBA limits ($l/D \leq 10$), nonequilibrium effects have been important. As indicated by the figure, only a few tests have been performed for large diameter pipes. (Essentially only the three limited sets of data shown exist.)

Figure A9.2

Map of Blow-down Flow Regimes



(After Figure I-3, 27 by permission.)

The second problem is that the ℓ/D ratio for the GE-DBA conditions is disturbingly close to unity. Figure A9.2 indicates that the vast majority of the data taken for pipes with ℓ/D values of approximately one have shown non-equilibrium results. If the GE physical arguments about the length being the controlling factor are not valid, then flow rates for such relatively "short" pipes could be much higher than equilibrium models would suggest even when the "conservatism" of the Moody model is considered.

The regulatory staff tried to overcome this problem in their Concluding Statement by calling for more than one model of blowdown break flow to be used in analyzing critical flow in accordance with the revised criteria of their Proposed Rule (6, pp. 105-108). Thus the staff concluded that,

The critical flow model of Moody is appropriate for use in break spectrum analysis of blowdown transients in BWRs and PWRs on the basis that it overpredicts blowdown flow...whenever the break exit plane quality is greater than about two percent...However, for the blowdown period during which subcooled liquid, saturated liquid or low quality two-phase fluid exists at the break exit plane, the Moody model underpredicts experimental discharge data...Therefore, the Proposed Rule requires the use of a model which is more appropriate to these fluid conditions. One such model contained in the evidence of this proceeding is the modified Zaloudek model of Westinghouse (Exhibit 1151, (57) Section III). The Moody model may also be applicable for early times during blowdown before the exit plane quality reaches two percent if it is used with a Moody multiplier of greater than unity... The staff concludes on the basis of this evidence that models appropriate to those flow regimes do exist. However, additional information describing how those models are incorporated in the computer programs should be evaluated in accordance with the proposed Section III.A of Appendix K (6, p. 105) (emphasis added).

In its revised AC, the Commission has downgraded the importance of the early blowdown stages where the need for a Moody multiplier

of greater than unity had been recognized. The AC now require that:

For all times after the discharging fluid has been calculated to be two-phase in composition, the discharge rate shall be calculated by use of the Moody Model... The calculation shall be conducted with at least three values of a discharge coefficient applied to the postulated break area, these values spanning the range from 0.6 to 1.0 (60, p. 1108).

Though the Commission acknowledged that recommendations for discharge coefficients greater than one were widespread, their discussion of the AC implies that the Moody model is always conservative and that the metastable flow period (if any) is adequately covered by Moody model results. They acknowledge that:

There was widespread agreement that a variable discharge coefficient provides a better fit to the data than a constant one.....Ybarrando (Transcript p. 6362) reported the result of an ANC calculation with a discharge coefficient initially 2.0, and later in blowdown 0.6, where the first peak in the clad temperature exceeded by about 100°F the value obtained with a fixed discharge coefficient of 1.0 (60, pp. 1111, 1112) (emphasis added).

Nevertheless, the Commission concluded:

We agree with the Staff position as to correctness of use of the model based on critical flow, since the length of time available during blowdown far exceeds the amount needed for nucleation and build-up of two-phase discharge. Furthermore, the evidence is strong that use of the Moody correlation does not underestimate observed experimental discharge rates, as would be the case if discharge were really metastable, but in fact it definitely overestimates the discharge rates (60, p. 1112).

The question of the adequacy of the experimental data was not addressed in any significant manner in the AC discussion.

In summary, it appears that the Moody break flow model may underpredict nonequilibrium flow by nearly a factor of two as indicated by the CNI. To compensate for this, the regulatory staff in the PR

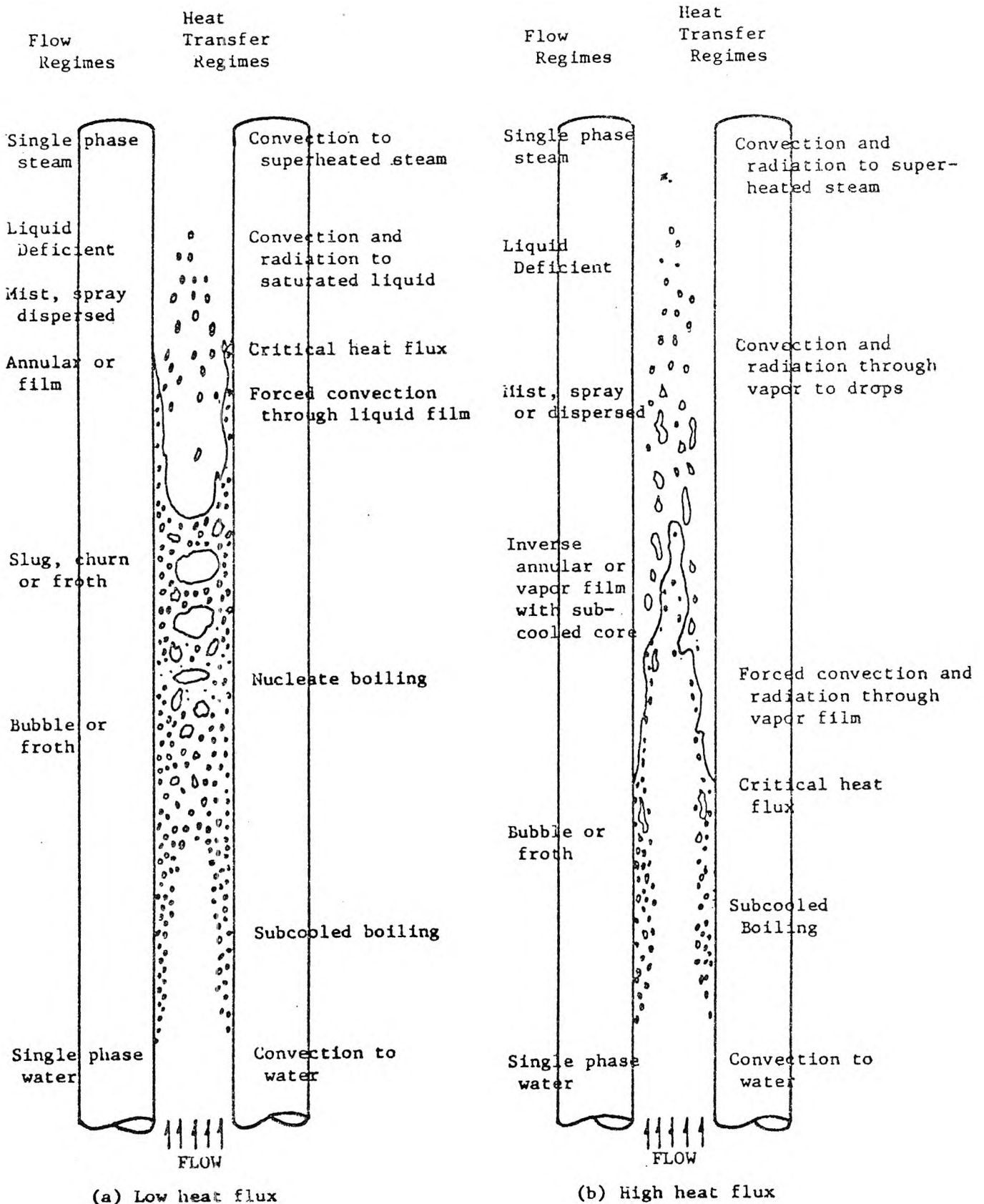
suggested using a different (but only vaguely identified) model which would estimate flow rates greater than those given by the Moody Model when the quantity of the exit fluid was 2 percent or less. In the final AC, the Commission downgraded the staff recommendation, implying that the Moody model (with discharge coefficients ≤ 1) was always conservative.

In view of the limited experimental basis for models which may be applied to the large diameter pipes associated with the DBA for large operational reactors, it would appear desirable to perform additional break flow tests with more representatively sized equipment. In the absence of experimental data which clearly supports the Moody model or indicates the requirement for a model of nonequilibrium flow in large pipes, more conservative and definitive specifications should be given in the criteria (e.g., quantified Moody multiplier's greater than one, with their period of application definitively specified) to attempt to assure that conservatism is attained for break flow specifications.

A9.3 Transient Critical Heat Flux and Heat Transfer

At the beginning of the LOCA transient, heat is transferred from the fuel rods to the coolant water in a continuation of the highly efficient nucleate boiling heat transfer mode of normal reactor operations. Heat transfer coefficients under nucleate boiling conditions are immensely higher (approximately a factor of 10,000) than those which occur during the core spray or initial reflood portions of the cooling process. During the rapid system decompression accompanying blowdown, a transition in the boiling process takes place from pinpoint nucleate boiling to film boiling and two-phase (liquid-vapor) convective cooling which greatly reduces the system cooling capability. Figure A9.3, reproduced from reference 33, schematically depicts the various boiling flow regimes for steady vertical up-flow of coolant.

Figure A9.3



Flow and Heat Transfer Regimes in Rods with Vertical Upflow

(After Figure III-1, 27.)

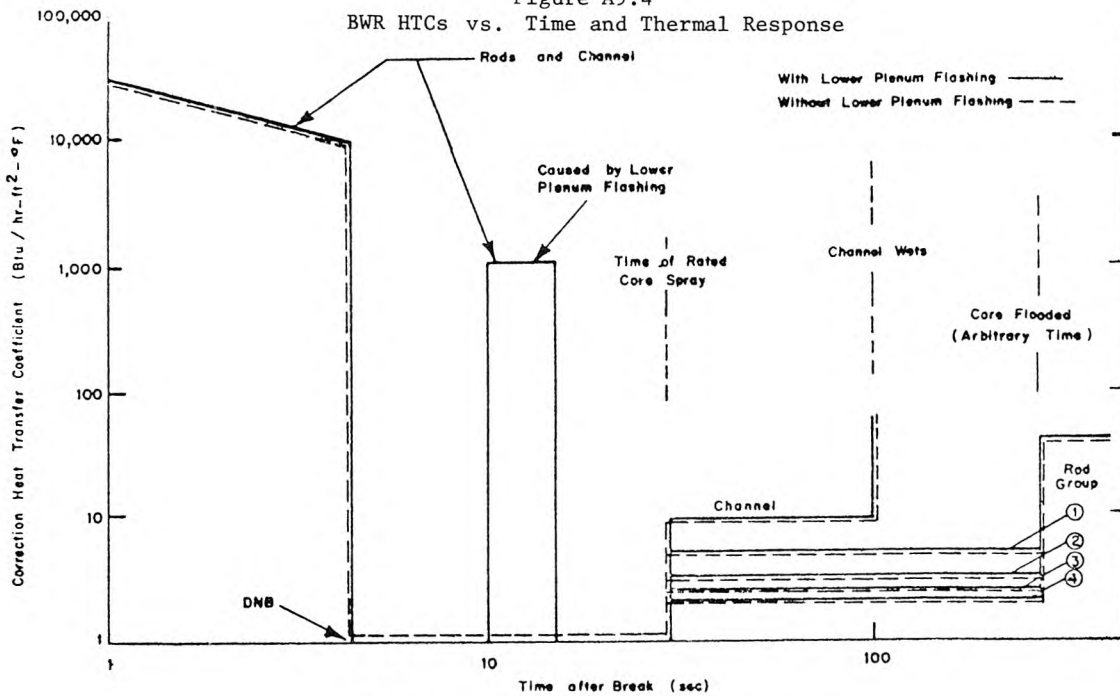
In describing the LOCA transient, analysis methods depend upon the development of the concept of time periods during which a given boiling transitional condition occurs (e.g., time to critical heat flux (CHF), or similarly, time to departure from nucleate boiling (DNB), or duration of stable film boiling). The important transition in the blowdown period is the first departure from nucleate boiling. This transition achieves its importance because the rapid rod dryout accompanying the transition makes reestablishment of the high heat transfer, nucleate boiling conditions for the rod very difficult. Moreover, the conditions for reestablishment are uncertain. As a result of the uncertainty over conditions leading to reestablishment of nucleate boiling (rewetting), the revised criterion conservatively require that clad rewetting phenomena during blowdown be neglected immediately after CHF is first predicted. The revised AC state:

After CHF is first predicted at an axial fuel rod location during blowdown, the calculation shall not use nucleate boiling heat transfer correlations at that location subsequently during the blowdown even if the calculated local fluid and surface conditions would apparently justify the reestablishment of nucleate boiling. Heat transfer assumptions characteristic of return to nuclear boiling (rewetting) shall be permitted when justified by the calculated local fluid and surface conditions during the reflood portion of a LOCA (60, p. 1109).

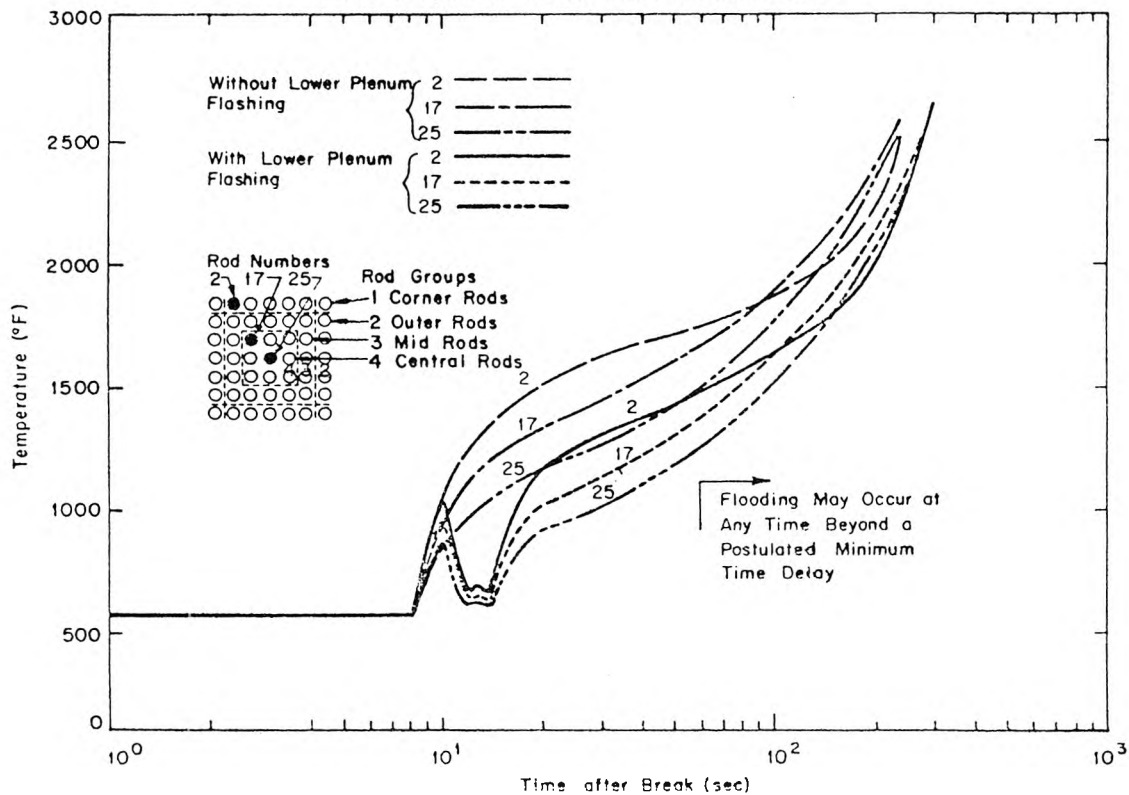
Neglect of rewetting during blowdown after DNB, was apparently also practiced under the IAC and precludes redevelopment of nucleate boiling conditions before reflooding begins.

Figure A9.4 (a composite of two figures, 14 and 15, from 10) shows typical assumptions for heat transfer coefficients (HTC) for a BWR throughout the LOCA thermal transient as well as the calculated thermal time history for the assumed values of the coefficients. The indicated heat transfer coefficients are very similar to the values prescribed by the IAC.

Figure A9.4
BWR HTCs vs. Time and Thermal Response



Two case assumptions of BWR-LOCA heat transfer coefficient response.



Calculated BWR transient temperature response for two case assumptions of LOCA heat transfer.
(After Figures 14 and 15, 10)

In reviewing the time phasing of the LOCA process from figure A9.4, the very high initial HTC's associated with nucleate boiling may be seen as a continuation of essentially normal heat transfer immediately following LOCA initiation. When DNB occurs, the HTC is conservatively assumed to go to zero, even though some transitional period with intermediate HTC prior to dry-out might be physically expected. The time for initial DNB is very critical. If the energy stored within the fuel followed predictions, but no blowdown heat transfer were to occur, rod temperatures would reach about 2000°F in 4 or 5 seconds as a result of internal temperature redistribution. After this time period, in the continued absence of blowdown heat transfer, a relatively slow temperature increase would occur, with an adiabatic heatup rate of about 20°F/sec to be expected in the high-power density regions of the reactor core at about 30 seconds after rupture (10, p. 310). In an alternative way of looking at the consequences of DNB, if the DNB transition can be avoided the high heat rates associated with nucleate boiling cause a reduction in the stored energy of the fuel (and hence of the potential for heating the cladding) corresponding to an average temperature decline of about 150°F/sec. Each additional second of nucleate boiling during depressurization can permit a delay of about 10 sec in core coolant (ECC) injection (34).

Comparing the HTC's and thermal response of the a) and b) portions of figure A9.4 respectively, it can be seen that as long as nucleate boiling is maintained during the blowdown there is essentially no change in temperature from that of normal operating conditions. DNB in a BWR is probably delayed several seconds beyond that which might normally occur in a PWR, until the rapidly falling coolant level in the downcomers uncovers the tops of the jet pumps. However, as soon as the transition through CHF occurs, the rod temperatures rise very rapidly as the stored energy in fuel causes the rod temperature redistribution to occur in the assumed absence ($HTC = 0$) of any convective heat transfer contribution.

When the fluid in the core falls to the level where the break in the recirculation drive pump line is finally exposed (conditions for the DBA), the reactor vessel depressurization takes place much more rapidly. The rapidly falling pressures cause the remaining water in the plenum chamber beneath the core to experience flash boiling. The strong flashing action forces the remaining water through the core at a rate initially equivalent to about 60 percent of normal core flow. The flash boiling induced flow continues, at a gradually decreasing rate, until the fluid in the lower plenum is essentially all expended. During this period of lower plenum flashing, calculations show the CHF is exceeded and theoretically the rods would be rewetted and nucleate boiling reestablished in the core. Uncertainty over the validity of this recurrence causes a degraded heat transfer coefficient, more representative of lower quality, film boiling HTC's ($100\text{--}1000\text{ B/hr-ft}^2\text{-}^\circ\text{F}$), to be used for estimating thermal response during the lower plenum flashing period.

Figure A9.4(b) depicts the thermal response for two possible cases, one without benefit from lower plenum flashing and the other assuming essentially standard film boiling HTC conditions during the period, as indicated in figure A9.4(a). The effect of the assumed film boiling conditions on the thermal response is very profound. As flashing occurs, a sharp temperature turnaround is induced for the brief flashing period (reducing the rod temperatures by $300\text{--}400^\circ\text{F}$ to near normal operating conditions). As the core flow decreases with approaching lower plenum exhaustion, the heat flux again exceeds CHF levels and complete loss of benefit from any form of boiling is assumed again (i.e., $\text{HTC} = 0$). This condition remains until core spray action is achieved (in figure A9.4, in 40 sec). Note that in the case assuming lower plenum flashing, a large thermal readjustment occurs again when CHF is exceeded and the HTC goes to zero. However, sufficient heat has been transferred in the brief period of lower plenum flashing (20 seconds) to reduce the maximum rod temperatures, as the rod thermal gradients begin to stabilize, to values approximately 400°F below the case where no benefit was attained

from flashing. Thus figure A9.4 demonstrated the very significant influence of boiling heat transfer coefficients on the thermal response of the fuel rods. The concept of CHF values and the times at which heat flux transitions through CHF occur have important effects upon the thermal response of the reactor.

The importance of the heat transfer occurring after CHF or departure from nucleate boiling (DNB) has been recognized by all those who have investigated the LOCA. In its discussion of the AC, the Commission stated:

The rate at which heat is transferred from the clad to the water after departure from nucleate boiling (DNB) is vital to estimation of the course of a hypothetical loss-of-coolant accident for a PWR. DNB is calculated to occur within about a tenth of a second after a postulated instantaneous double-ended break of a large pipe, or a large split. The heat transfer after this time would primarily determine the temperature history of the clad during blowdown and the possibility that clad damage would occur during this phase. It would also determine the effectiveness of removal of heat from the oxide fuel itself and thus the stored energy in the fuel at the time refill of the plenum by ECCS fluid starts (60, p. 1117) (emphasis added).

Though a significant body of experimental work has been done on CHF investigations, the most reliable material is based upon steady-state fluid conditions. Consequently, the AEC's new AC has authorized "conservative" application of steady-state data to transient LOCA conditions (60, p. 1118).

The use of steady state correlations for defining stable film boiling after CHF has been questioned. However, in its discussion of the AC, the Commission has strongly supported the conservatism of use of steady-state correlations. They stated:

Transient Heat Transfer: Some criticism was expressed as to the use of steady state correlations during the fast

transients analyzed for a large LOCA. These views were stated by Lawson (Transcript, pp. 5766-7), Ybarrondo (Transcript, pp. 6069, 10282, 10890, 10906-7), and Brockett (pp. 7480, 7588). The tenor of the criticism was that evidence was not conclusive that steady state correlations overpredicted the transient coefficients or predicted them accurately.

Considerable evidence was provided nonetheless to the effect that during depressurization the use of steady state correlations for stable film boiling was a conservative course.....In our view the evidence is near overwhelming that the use of steady state correlations for stable film boiling after CHF will provide a conservative estimate of heat transfer during blowdown (60, p. 1118) (emphasis added).

In spite of the Commission's assertions of "overwhelming" evidence of conservatism, not all of those who criticized the use of these coefficients are entirely convinced. In hearings before the Joint Committee on Atomic Energy (22-24 January 1974), Ybarrondo, manager of the LOFT tests for ANC, stated:

In Section III C, 5 of the Opinion of the Commission, transient heat transfer is discussed. An experimental program directed at providing data relevant to quantifying the margins resulting from using steady state correlations during post-critical-heat-flux heat transfer would be very valuable. This is especially true in view of the commission's statement on page 99, "The rate at which heat is transferred from the clad to the water after departure from nucleate boiling (DNB) is vital to estimation of the course of a hypothetical loss-of-coolant accident for a PWR," in which I concur (59, p. 5501) (emphasis added).

However, even under steady state conditions, data for full length (12 ft.) large arrays (7 x 7 or greater) of PWR rod diameters "are not now available for multirod bundles of this size and geometry..." (34).

Although the GE transient CHF correlation (53, p. C-9) was approved for LOCA analyses, the AEC qualified their acceptance for the method by requiring demonstrated conservatism through submission by the

vendor of a "statistical uncertainty analysis" of the data and its application in the transient correlation. In the words of the AEC staff:

These procedures can be characterized as requiring conservatism in the treatment of transient CHF data. The reason for requiring this conservatism is related to the sources of uncertainty in the realistic prediction of transient CHF (6, p. 133).

Freely translated, this statement seems to imply that the existing transient CHF data sources are of uncertain validity. The CNI have observed that experiments to

precisely predict heat transfer coefficients for parallel pin arrays that extend over the range of fuel-pin geometries and coolant conditions that exist during blowdowns....[have not] been completed by the AEC and none yet appears to be forthcoming. One major research program on blowdown heat transfer that the Commission is presently funding is a joint program with General Electric Company. However a review of that program by ANC stated that it does not relate to the problems that need to be solved and that the program is not, in ANC's opinion, relevant to the needs of the AEC and the nuclear industry (HAI -16-71, Jan. 26, 1971) (6, p. 4.15).

The problems with the transient CHF correlations are felt to exist primarily because current models for their application are highly empirical. Serious attempts to correlate the experimental data with theoretical or analytical methods have apparently not been made. Consequently, substantial uncertainties exist with regard to application and scalability of the experimental results to the conditions of the fluid in a reactor core during a LOCA. The current ORNL combined experimental and analytical program on large bundles of full scale fuel rods (described in 34) may help to resolve the heavy dependence upon pure empiricism. One of the stated objectives of the program is

to obtain data on the thermal response of the heater surfaces in the above multirod bundle during the first few seconds of a blowdown transient comparable with the

LOCA transient associated with a double-ended inlet-pipe break in a PWR. These experiments will establish the time to reach CHF, the magnitude of CHF, and heat transfer rates in the immediate post-CHF period; the results will quantify both the sequence and magnitude of thermal-hydraulic events during a LOCA-type depressurization and, through comparison, the conservatism associated with existing calculational models for estimating CHF and post-CHF heat transfer (34).

Such data, when combined and correlated with other current experimental and analytical programs and the results of applications of the revised AEC criteria requirements may help to resolve some of the remaining uncertainty in CHF related reactor thermal response.

A9.4 Reflood Heat Transfer Parameter Evaluation

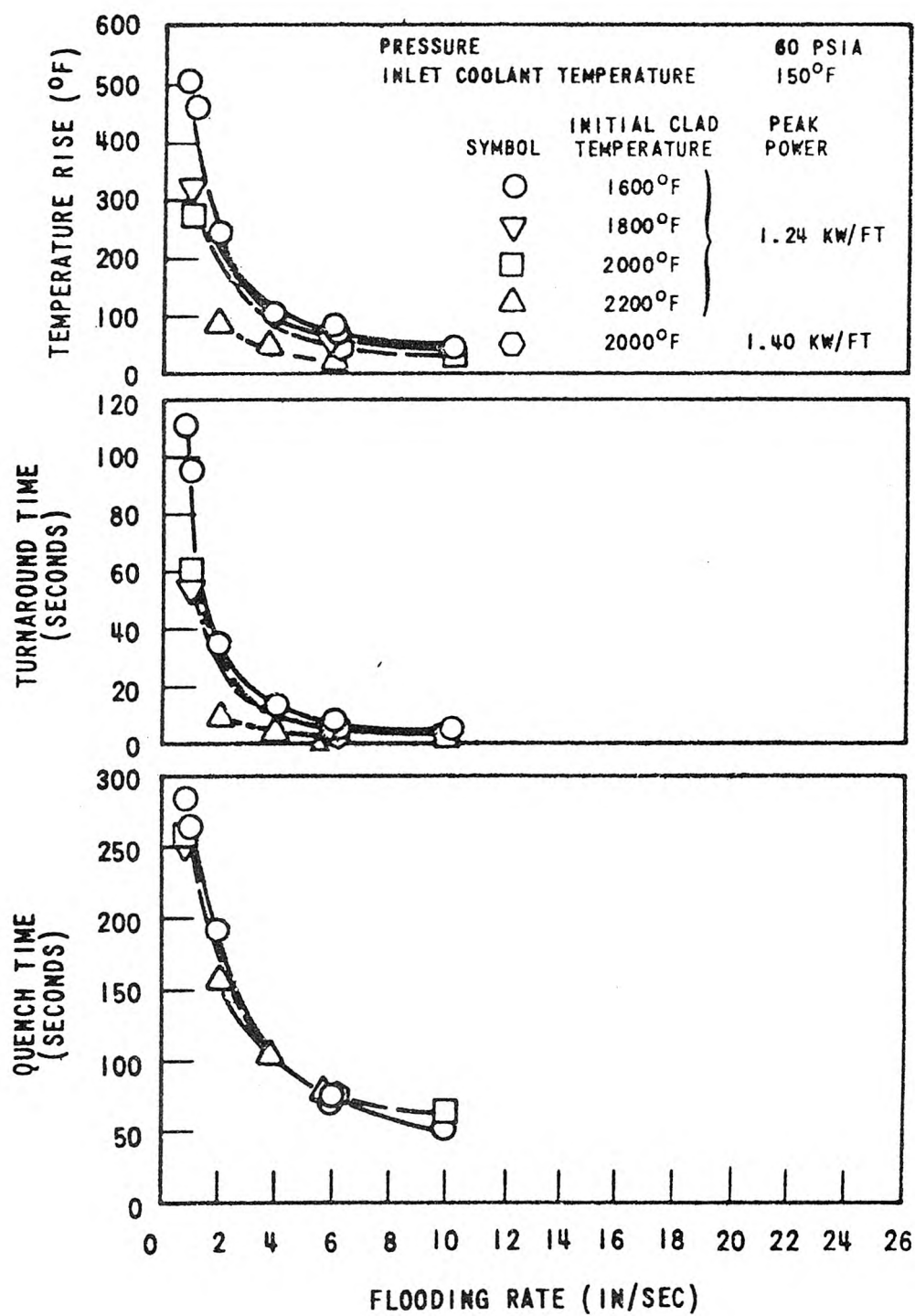
The reflood heat transfer mechanisms, for both PWRs and BWRs, appear to be the dominating factors with respect to the reliability of the ECCS. If sufficient cooling water can be supplied to the core, FLECHT tests have demonstrated core coolability for initial reflood temperatures ranging to near the AC limits of 2200°F. The adequacy of reflooding rates depends critically upon questions of flow resistance in the reactor primary loops for PWRs. Local heat transfer during reflood is also strongly influenced by local and general blockage of the core induced by fuel rod swelling and rupture. In the PWR, steam generator tube failure (ruled outside the scope of the ECCS hearings) may also strongly increase primary loop resistances with consequent substantial reductions in reflood rates. This section will review the significance of these parameters.

A9.4.1 Empirical estimates of reflood heat transfer (FLECHT)

Figures A9.5 and A9.6, reproduced from the PWR-FLECHT Final Report (50), summarize FLECHT results in terms of temperature rise, turnaround time, and quench time as functions of flooding rate and peak rod decay power respectively. The strong dependency of the results on flooding rate is shown clearly in figure A9.5, At flooding rates greater

Figure A9.5

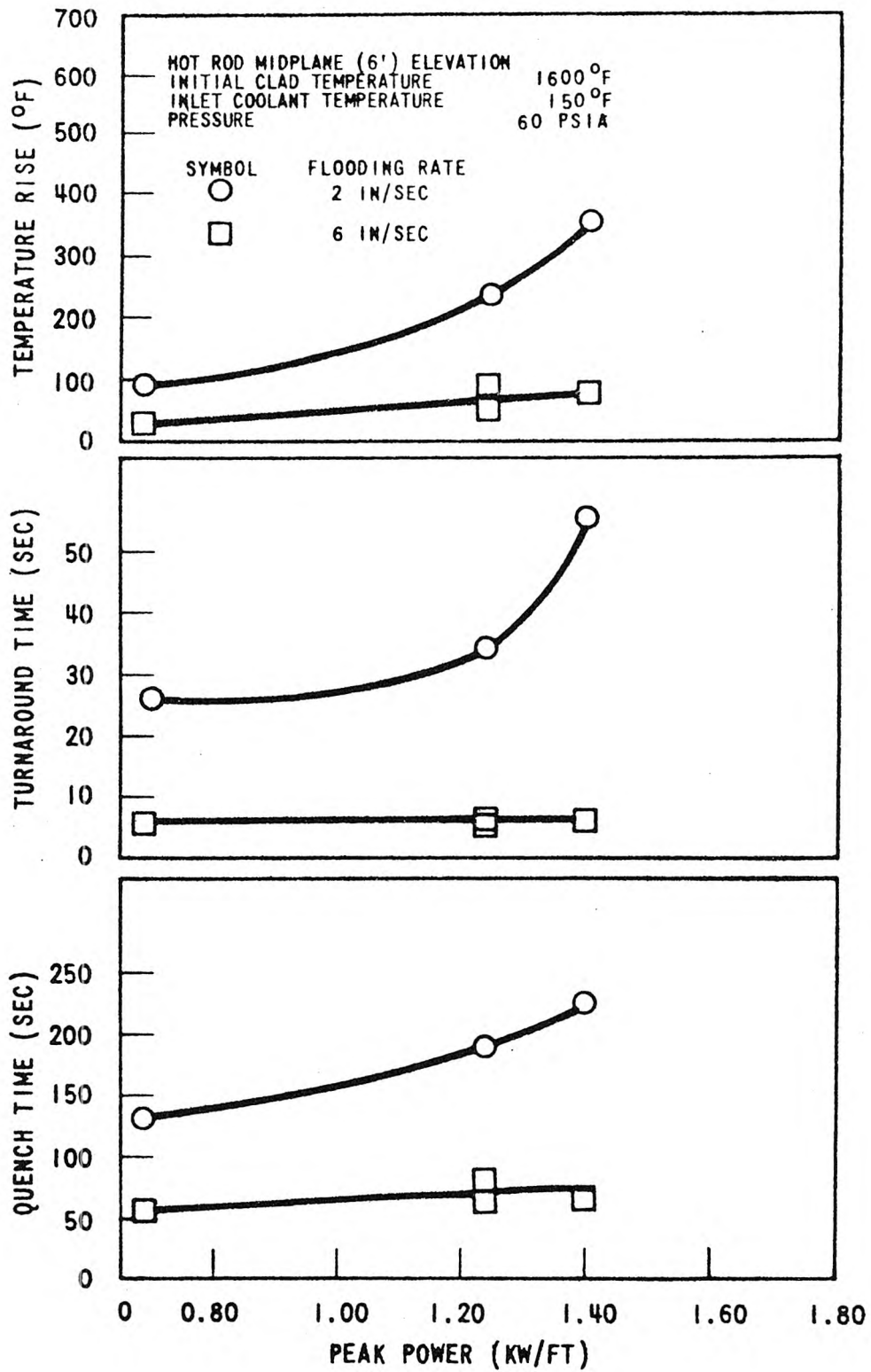
Summary of PWR-FLECHT Results as a Function of Flooding Rate



(After Figure 3-13, 50, by permission)

Figure A9.6

Summary of PWR-FLECHT Results as a Function of Rod Decay Power



(After Figure 3-20, 50, by permission)

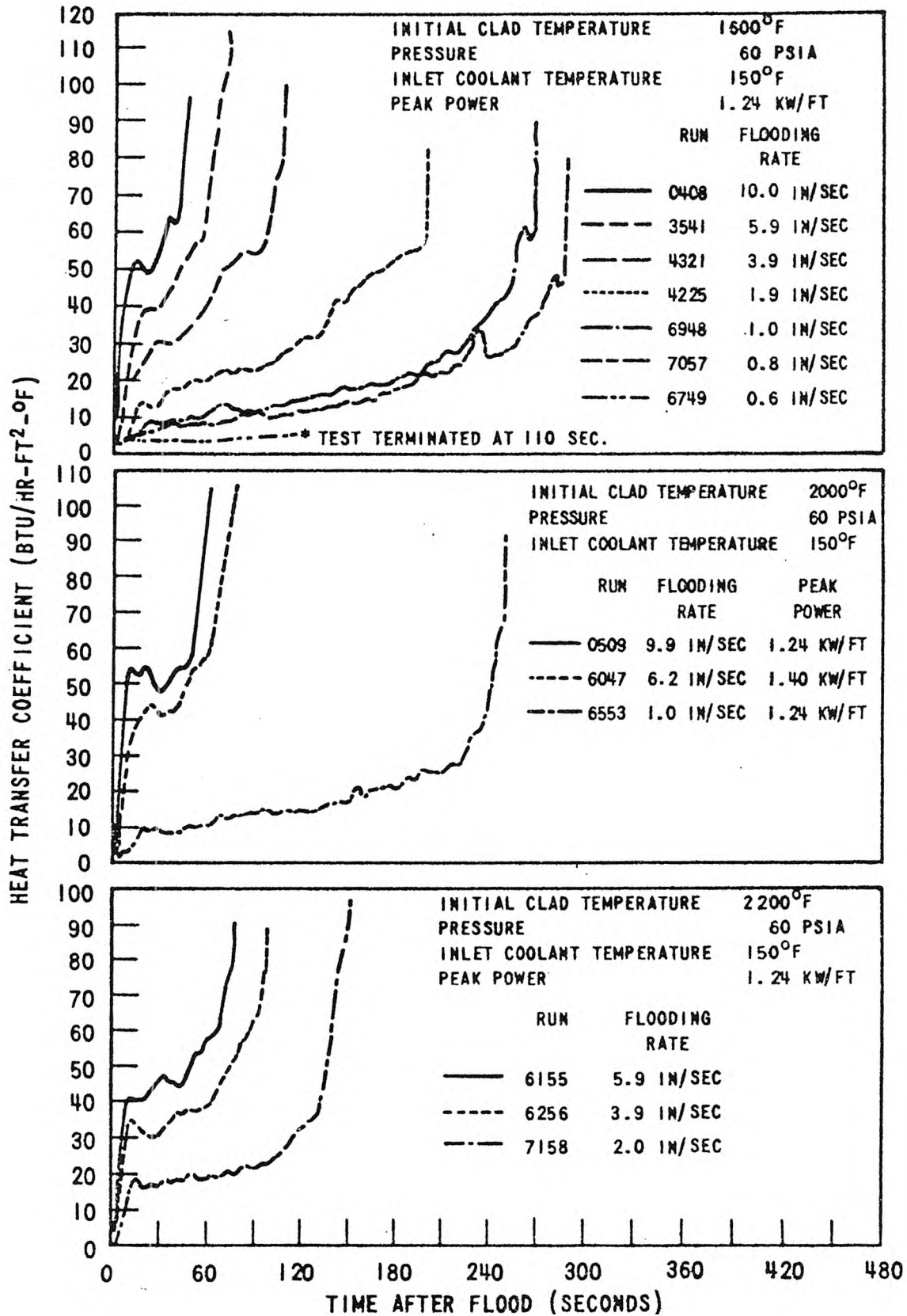
than 4 in/sec, temperature rise, turnaround time, and quench time were all minimal. At flooding rates less than 4 in/sec, a clear transition in all parameters can be observed. At rates on the order of 2 in/sec or less, temperature rise and times to turnaround and quench increase strongly with decreasing flood rate. The results imply that for flood rates of 2 in/sec or less controllability problems are greatly exacerbated. The results clearly indicate a great advantage in attaining flooding rates in excess of 4 in/sec.

At high flooding rates (on the order of 6 in/sec), temperature rise, turnaround time, and quench time as shown in figure A9.6 are weak functions of initial rod decay power. However, as flooding rates decrease, the influence of initial rod power becomes increasingly important. Again the message of the figure is clear; if rod decay power is uncertain, flooding rates of the order of 6 in/sec are desirable to minimize the effects of uncertainties in this parameter. Extrapolation of the results to low flooding rates (2 in/sec or lower) implies that uncertainties in rod decay power may be dramatically amplified and a 10-15 percent uncertainty in initial rod power could result in a peak temperature increase of approximately 100°F, assuming no synergistic effects from other variables produced even higher temperatures.

The time histories of convective heat transfer coefficients (HTC) derived from the tests are shown in figure A9.7 for a variety of parameters, including initial temperature, flooding rate, and initial rod decay power. Comparison of figures A9.5 and A9.6 with figure A9.7 indicates that temperature turnaround was achieved when rod HTCs of 15-20 B/hr-ft²-°F were obtained. Similarly the rods were quenched at HTCs of about 40-50 B/hr-ft²-°F. The dramatic increases in heat transfer coefficients, towards nucleate boiling values, are apparent at the transition to rod quenching for each of the tests. When this transition occurred, the temperature transient for the rods was essentially terminated, almost at once.

Figure A9.7

Summary of PWR-FLECHT Heat Transfer Coefficient Results



(After Figure 3-12, 50, by permission.)

Analysis of the results shown in figure A9.7 indicates that after the fluid in the reactor plenum reaches the bottom of the fuel rods, a typical HTC-time history shows a rapid increase to a relatively stable (but gradually increasing) value which is maintained until the transition to quenching occurs. This relatively stable plateau HTC, hereafter referred to as the nominal initial reflood HTC, can be seen to increase strongly with increasing flood rate. At a flood rate of 1 in/sec, the nominal HTC is about 10 B/hr-ft²-°F, while at 4 in/sec it increased to approximately 30 B/hr-ft²-°F, and at 6 in/sec reaches a nominal value of 40 B/hr-ft²-°F in its initial rapid rise. At a nominal value of 40 B/hr-ft²-°F, temperature turnaround occurs almost at once, and rod quenching takes place in approximately one minute. Conversely, as implied by figure A9.7, at a flooding rate of 0.6 in/sec temperature turnaround could not be achieved at the nominal HTC of 3-4 B/hr-ft²-°F indicated, even though the initial rod temperature was a relatively low 1600°F. These low flooding rate results demonstrate the difficulty of thermal excursion control at rates less than 1 in/sec.

It should be observed that HTCs are not identical at all points on a given rod. As indicated in figure A9.7, results have been shown for the rod midplane (6 ft) values. At lower rod elevations, HTCs are greater than those shown, while at higher levels they tend to decrease. However, thermal hot spots and peak rod powers generally occur at the midplane location and the results shown are representative of typical values for the rods.

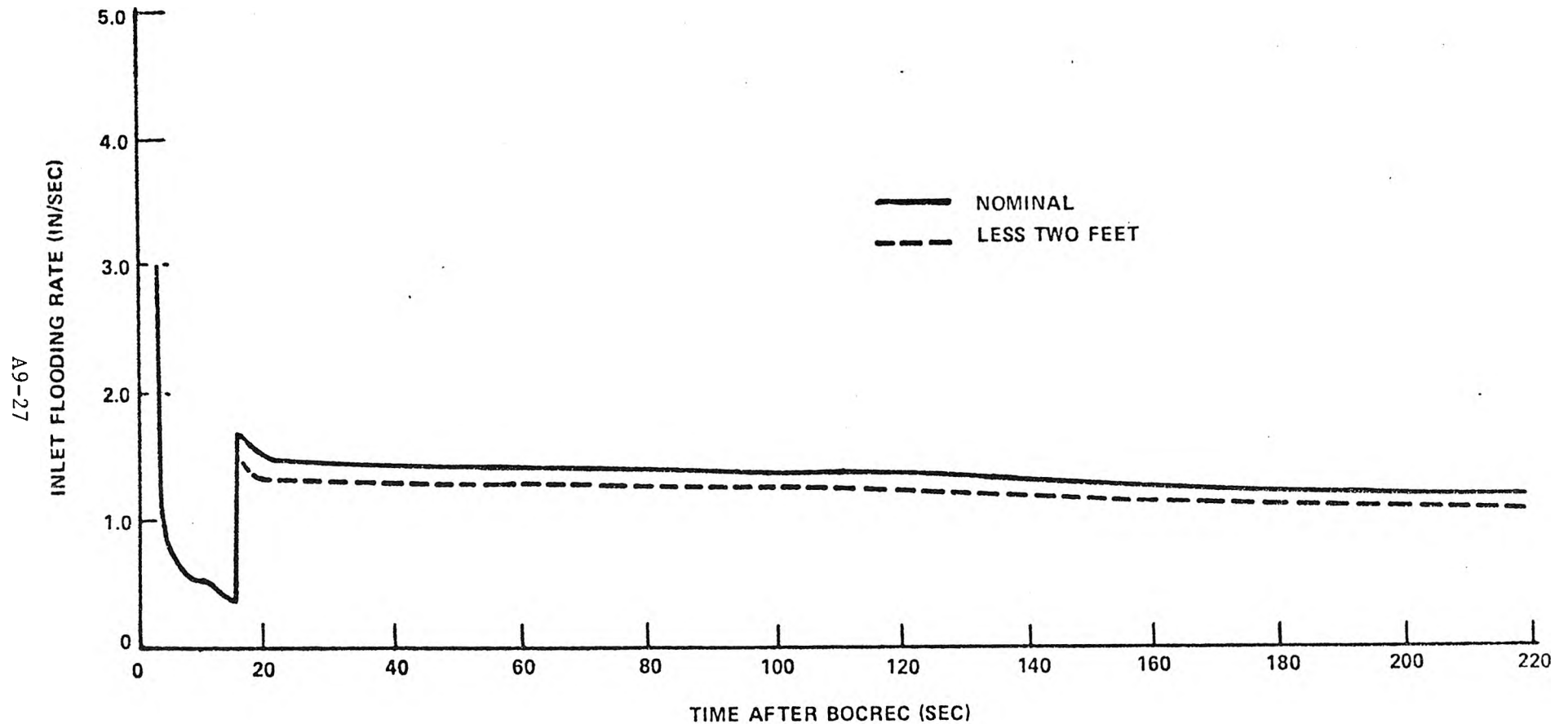
The principal observation to be made from the FLECHT results is that an important transition occurs in reflood thermal transient control at flood rates in excess of approximately 4 in/sec. At flood rates above this value, temperature rise, turnaround time, and quench time are minimized. Conversely, at flood rates on the order of 1 in/sec, thermal transient controllability problems are substantially magnified.

A9.4.2 Reflooding rate predictions

The AEC estimate of nominal reflood rates for a typical four loop PWR is shown in figure A9.8, reproduced from the AEC Supplemental Testimony (4, p. 14-12). The results shown indicate an initial flood rate of about 3 in/sec falling in approximately 5 sec to a low of about 1/2 in/sec resulting from line plugging. The mechanism of line plugging is associated with ECC fluid filling inlet pipes which is assumed to block the passage of steam in the unbroken steam generator loops during ECCS accumulator injection into the unbroken cold leg pipes. The minimum reflood rate lasts until about 16 sec after the bottom of the core is first recovered by the entering ECC fluid (defined to be the beginning of core reflooding). After that 16 sec period of core reflooding, the ECCS accumulator injection is essentially completed. This minimal flood rate is followed by an increase to a pseudo steady state, slowly decaying flooding rate which is commonly called the nominal flooding rate for the reactor. For the case shown in figure A9.8, the nominal flood rate varies from about 1.4 in/sec at 20 sec to approximately 1.1 in/sec at 220 sec.

The results shown in figure A9.8 were obtained by the AEC using a calculational method (the Flood 1 computer code) developed by ANC, with which "the Regulatory Staff intends to evaluate plants" in the future (4, p. 14-10). The analysis was based upon "...a set of realistically calculated resistance(s), for system components, using assumptions set forth in the Interim Policy Statement (IAC)" (4, p. 14-6). Estimates of flooding rates for several other types of PWR nuclear steam systems were also made, based upon similar assumptions. Predicted values range from less than 1 in/sec (0.9 in/sec) for a Westinghouse four loop system with an ice condenser type containment (4, p. 14-7) to values as high as 2 in/sec for B & W vent-valve plants (4, p. 14-9).

Figure A9.8
AEC Estimates of Nominal Reflood Rates for Typical Four Loop PWR
EFFECT OF VARIATION OF 2 FEET IN THE DOWNCOMER HEIGHT
ON THE FLOODING RATES



Thus the range of "realistically" predicted reflooding rates, however conservative the IAC specifications may be felt to be, is narrow, running from less than 1 in/sec to only 2 in/sec with nominal rates typically about 1.3 in/sec for PWRs. Clearly these reflood rates are on the low side of desirable flooding rates, where uncertainties in flooding rates magnify potential problems with thermal excursions, as discussed in the previous section.

BWR reflood rates are apparently substantially higher than PWR rates, typically as high as 3.7 inches per second (60, p. 1125). Reflood HTC's of 25 B/hr-ft²-°F have been specified by the Commission (60, p. 1126). When compared with equivalent HTC's for PWRs, the BWR-HTC's correspond to flood rates of about 2-3 in/sec (e.g., figure A9.7), and are probably somewhat conservative for the indicated flood rates. Though BWR reflood rates appear somewhat higher than typical PWR rates, they are still slightly below the apparent critical transition points of desirable reflood heat transfer conditions.

The relatively low PWR reflood rates are generally attributed to problems with steam binding. In the discussion of the AC, the Commission suggests:

The reflooding rate for pressurized water reactors would be controlled to a large extent by steam binding, the phenomenon by which the resistance to flow through the reactor system (steam generators, pumps, etc.) of the effluent from the reactor core limits the rate of reflood and, indirectly, the rate of heat removal from the fuel rods. The pumps in their locked rotor condition would typically provide more than half of this resistance to flow so that the stipulation of their being locked is a serious limitation. If the pump rotors were not locked, their resistance to flow would be reduced by 60% (Exhibit 1113, p. 14-10). In their Concluding Statement, Combustion Engineering states that if the pumps were free running during reflood the calculated maximum temperature of the zircaloy cladding would be reduced by 75°F (CE Concluding Statement, p. 3-61).

The stipulation of locked pumps during reflood is unchanged from the Interim Policy Statement, and no new experimental information was provided during the hearing justifying a change in this part of the rule (60, p. 1122) (emphasis added).

The steam binding problem is exacerbated by the ECCS accumulator injection design. Only the accumulated head associated with the differential height of the fluid in the downcomer relative to the height in the core is available to drive the ECC fluid. It is opposed by the flow resistance developed in the unbroken legs of the ruptured nuclear steam supply systems. Reduced flood rates due to normal LOCA flow resistance for the system are additionally perturbed by such problems as fluid oscillations in the downcomer, density decreased in ECC fluid within the downcomer, and line plugging during accumulator injection. As a consequence, obtaining higher flooding rates with current PWR ECCS designs is probably not possible. System redesign might be needed to achieve flooding rates as high as the 4-6 in/sec which would appear to be desirable.

For BWRs there do not appear to be any inherent reactor design problems which would preclude obtaining higher flooding rates, if they were desired. Direct injection of the BWR-ECC reflooding fluid through the pressure vessel head above the core apparently minimizes flow resistance constraints to increasing reflood rates. Simplistically speaking, increased reflood rates would appear attainable through simply increasing the pump capacity for the BWR core flooding injection system.

Estimates of reflood rates are clouded by two major problems: inadequacies of current flood rate computational methods and lack of experimental evidence to support the predicted results. In the words of the AEC:

All three PWR vendor codes, as well as the FLOOD 1 code (the current AEC recommended model), are incomplete in their modeling to differing degrees, and all codes lack

experimental verification....In view of the present status of reflood codes and experimental programs, the need to develop or improve reflood computer codes is indicated and the staff considers this development can be accomplished relatively soon (4, p. 14-17).

The Commission, in their discussion of the AC, also acknowledged the need for reflood model improvement. They stated:

The Regulatory Staff in their Concluding Statement proposed the development of more sophisticated refill-reflood computer programs, including those capable of predicting the expected oscillatory flow of wake into the reactor core.....(The AEC anticipates that the "expected" oscillatory flow during reflood will improve reflood HTC.)....The Commission believes with the Staff that improved and more realistic models are desirable, but realizes that the full benefit of sophisticated models that predict oscillatory flow cannot be obtained until there are more suitable experiments with which they can be compared (60, p. 1122, 1123) (emphasis added).

Though the AEC staff and the Commission apparently feel such improvements would permit reduction in current levels of conservatism in the codes, with possible consequent increases in predicted reflood rates, the lack of experimental evidence to support such assumptions will make future implementation of reductions in conservatism difficult to support until the needed evidence is available.

A9.4.3 Flow blockage and core flow distribution

Swelling and rupture of fuel rods during the LOCA can perturb flow distribution of the ECC fluid away from local hot spots with consequent decreases in cooling and hence increases in local rod temperatures. The potential for core blockage has been a source of serious concern to the CNI. In their concluding statement they said,

Clad ductility for some pressure-temperature regimes is clearly sufficient to allow complete or nearly complete coolant channel closure....The results [of CNI calculations] establish that in the present state of knowledge flow blockage effects may well hopelessly compromise ECCS effectiveness (7, p. 5.3).

The AEC has acknowledged that their consultants have indicated that extensive core blockage could be expected. During the hearings, Rittenhouse testified "that (local) blockages of 90% embedded in (general core blockages of) 80%, '90% in 70%, 90% in 50% and 80% in 50% could be inferred by examination of the ORNL multi-rod burst tests" (4, p. 20-8).

Estimates of expected core blockage were given by the Commission in their discussion of the AC. They recognized that:

In the postulated LOCA the reactor system pressure would drop rapidly and would soon fall below the pressure of the helium and fission gases within the fuel rod. The resulting differential pressure would exert an expansive force on the cladding. At the same time, as the cooling effectiveness dropped, the temperature of the cladding would increase rapidly, decreasing the yield strength of the cladding. At some time during the LOCA the yield strength of the zircaloy might become less than the tensile stresses exerted by the differential pressure, and the cladding would then swell and perhaps burst.

For example, Babcock and Wilcox, using the evaluation model of the Interim Policy Statement, estimated that, for pressurized fuel, rupture of the cladding would be predicted over 70% of the core 1.3 seconds after the maximum size cold leg break. (Exhibit 1059, p. 6-4.) This corresponds to the time when the differential pressure would be about 200 psi and the cladding temperature about 1800°F. Westinghouse in a similar calculation, conservatively estimated that 25% of the fuel rods would burst sometime during blowdown, and that, by the end of the reflood period, 70% of the rods would burst. (Exhibit 1078, pp. D-48 and D-49.) Combustion Engineering calculated the degree of flow blockage resulting from rod swelling for each fuel assembly in the core for both unpressurized and pressurized fuel. In both cases, as judged from the blockage, they were calculated to be perforated or swollen rods in nearly every fuel assembly. The major difference between the pressurized and unpressurized fuel was that the unpressurized fuel was estimated to undergo less swelling and perforation during blowdown, as of course might be expected. (Exhibit 1144, sec. 5, using material from Exhibit 1066, sec. 2.)

For the Boiling Water Reactor the situation seems to be somewhat different. The blowdown would provide a longer period of assured effective cooling of the fuel elements, and the initial calculated rise in temperature of the cladding is not so great. Furthermore, the pressure within the fuel rods is said to be low, so that ballooning of the cladding would not be expected to occur during the blowdown (Exhibit 1001), p. 2-24). General Electric offered one calculation for a 1967 product line BWR for which the peak cladding temperature was 2105°F (Exhibit 1148, sec. P). Using some of the assumptions made by CNI (Exhibit 1041, sec. 7.2), but using a constant internal fuel rod pressure, they calculated that 13% of the rods in the hottest bundle would perforate. CNI, using the probably erroneous assumption that there was no communication between the hot spot and the fission gas plenum at the top of the fuel rod, estimated that 22% of all the fuel rods in the whole reactor would rupture. They said that this compares with 21% estimated by General Electric for the Pilgrim reactor (Exhibit 1041, p. 7.9). In Exhibit 1032, Page II.8.2-1, reference is made to a calculation for a Boiling Water Reactor in which 60% of the fuel pins were expected to rupture by the time the ECCS core sprays came on, with 75% of the pins expected to rupture ultimately.....

From the above it is obvious that, when the course of the LOCA is calculated according to the conservative prescriptions of an approved evaluation model, swelling and bursting of the cladding will be estimated to occur in abundance (60, pp. 1104, 1105) (emphasis added).

The location of swelling on the rods and relative inter-rod relationship of swollen segments is as important to blockage as the overall extent of swollen and ruptured rods. This aspect of the core blockage question has been a subject of substantial controversy. It apparently will be a continuing center of uncertainty and debate, as we will discuss below. In their AC discussion the Commission noted:

The data from the rod burst tests show a great deal of scatter, particularly in the degree of swelling experienced. (See, for example, fig 2.5 of Exhibit 1066.) The greatest controversy, however, has been with respect to interpretations and predictions of the resulting blockage to coolant flow.....It is expected that variations in

cladding thickness, fuel pellet properties, gap thickness and eccentricity, and the texture of the zircaloy because of its anisotropic character would go into the determination of just where along the length of a fuel rod the perforation would occur (Transcript 11, 515-18).

The swollen and perforated region is expected to be about 1-1/2 to 3 inches long, and to occur at random as determined by the above variables over a length of relatively uniform temperature of from 7 inches to 27 inches. (Trans. 12, 701; Exhibit 1066, fig. 2.7; Exhibit 1144, p. 5.2). Thus it is not expected that a large number of adjacent rods would have their maximum swelling in the same plane. The maximum blockage observed to date in any multirod experiment containing 16 channels or more has been approximately 70% on any horizontal plane (Trans. pp. 9166-7).

All in all, the record still supports the Regulatory Staff position in Exhibit 1001, pp. 2-12, namely, that the core-wide flow area reduction in the plane of greatest blockage would not exceed 60% and that local flow channel reductions, over perhaps a 4 x 4 array of fuel rods, would not exceed 90%. As shown by calculation of blowdown and ECCS heat transfer considered elsewhere, these reductions of flow area, while necessary to be considered, would not be disastrous. In other words, the Commission concludes that estimated fuel rod swelling and rupture would not render the geometry of the core to be uncoolable (60, pp. 1105, 1106) (emphasis added).

That the question of the extent of blockage may not have been resolved with adequate conservatism is demonstrated by the testimony of William B. Cottrell, Director of the Nuclear Safety Information Center, Oak Ridge National Laboratory.

Two statements in the Commission's discussion of the criteria....., if not clarified, could result in a lack of conservatisms in applying the criteria...

The question of core flow blockage as results from fuel pin swelling is addressed only indirectly in new criterion No. 4 but is adequately covered later in the criteria in the discussion of evaluation models (Section IB). There it is stated that clad swelling and rupture must be considered, and "shall be based upon applicable data in such a way that...(the effects) are not underestimated."

It should be realized that only little applicable data exists in this area and that which is available is open to question. In this regard there are two statements in Section IIIB of the Opinion of the Commission which are of concern: specifically, (1) it is assumed that clad swelling and perforations occur at random over a length of from 7 to 27 inches. I should point out the shorter the length, the more compact the blockage and, therefore, the greater difficulty in cooling the reactor and secondly, undue significance is given to the maximum observed blockage of 70 percent to support the Regulatory Staff position that local flow reductions would not exceed 90 percent.

The available information could equally well, if not better, justify a random failure distribution over less than seven inches in length and local flow blockages in excess of 90 percent. These are both important matters, which should be resolved by additional experiments.

Furthermore, until such time as definitive information on rod swelling behavior is available, I would recommend that highly conservative blockage values be used in the calculation models (59, pp. 323-325).

Cottrell's position on the need for utilization of more conservative blockage values was further amplified by the testimony of his associate at ORNL, Phillip Rittenhouse. Rittenhouse cautioned with regard to the AC requirement for predicting clad swelling and rupture:

I have one caution in this regard. The Commission also states these calculations should be based on applicable data in such a way that the swelling and rupture are not underestimated.

I have some concern that we presently have inadequate data to do this. I would caution that in the use of that applicable data that these evaluation models be defined as that presently available and giving the most conservative result.

The Commission also discusses blockages resulting from swelling and rupture, prescribes how the effects of the blockage should be considered during blowdown and on heat transfer during re-fill and re-flood period.

I agree with the AEC's stated position that core-wide flow area reduction will not exceed 60 percent. However, I do not believe that the 90 percent limit on local flow channel reduction, four by four array of rods defined by the Commission has been satisfactorily demonstrated.

I suggest that this is a subject that needs additional study to determine the magnitude of flow blockage and for the present we assume no less than 95 percent local flow blockage (59, pp. 340, 342) (emphasis added).

The effects of such extensive channel blockage and the resultant flow redistribution have been variously estimated for PWRs. With evaluation models approved under the IAC, vendors were allowed to estimate hot channel flow as 80 percent of the "smoothed" calculated average flow for the core. Under the PR, it was proposed that flow diversion for a "hot region" no larger than the "size of one fuel assembly" be calculated, allowing for blockage induced crossflow resulting from clad swelling or rupture. Somewhat similarly to the IAC prescription, the PR recommended that the hot region flow, during blowdown, be "multiplied" by a flow reduction factor of 0.8 "to allow for the effects of clad swelling or rupture" (6, p. 51).

The 80 percent of average core flow factor applied in the IAC to estimate hot channel flow was acknowledged by the AEC to be "an arbitrary, interim factor intended to compensate for uncertainties in core flow distribution" (4, p. 7-7). More sophisticated calculations of hot channel flow were said to indicate flow reductions ranging from a factor of 0.9 times average flow for single phase steam flow to complete stagnation for two phase (steam-liquid) flow under certain conditions (4, p. 7-6). Parametric studies of the effect of flow diversion on the hot channel temperature excursion have indicated a temperature increase of approximately 470°F as flow decreased from 100 percent to 40 percent average (4, p. 7-11). Though the sensitivity of the thermal excursion due to blockage was minimized by the regulatory staff (the change was

only "about 8°F per one percent change in core flow" (4), the large uncertainty in predicted factors for flow diversion (including total flow stagnation) must make this parameter non-negligible in estimating the effects of core blockage on blowdown heat transfer.

An extensive discussion of blowdown effect induced by core blockage was included in the discussion of the AC. The Commission stated:

The analytical models used in reviewing the course of a hypothetical loss of coolant accident under the Interim Policy Statement have all been one-dimensional, with no direct treatment of flow redistribution in the core. The detailed flow in the reactor core following initiation of the hypothetical loss-of-coolant would be complex, and would be different depending on the kind of reactor and fuel and the specific time during the LOCA. The nature and degree of flow redistribution were discussed at length during the hearing.

Flow redistribution between channels is a phenomenon primarily affecting Pressurized Water Reactors, because they have no channel walls to restrict cross-flow. The principal forces affecting cross-flow are friction, acceleration, drag in channels, drag through spacer grids and fittings, and buoyancy (Morgan, Transcript pp. 12678-9). The presence of a two-phase fluid affects buoyancy and frictional drag (through the two-phase multipliers). In upflow the buoyancy effects tend to produce higher hot channel flow than average channel flow (though frictional effects act in the opposite direction). In downflow the buoyancy and the friction act together to reduce hot channel flow relative to average channel flow (Morgan, Transcript 12679).

The Interim Acceptance Criteria models have accounted for these effects by a requirement that the average channel flow during blowdown of a PWR be multiplied by a factor of 0.8 to obtain the flow in the hot channel calculation. Westinghouse Testimony stated that calculations made using the THINC code are the basis for this choice of factor. The calculation assumed parallel channels, and zero cross-flow resistance between channels, so that the pressure was constant in every horizontal plane. These calculations did not unambiguously lead to flow reductions bounded by the factor 0.8.

Although the vendors' discussions of the effect of flow redistribution generally tended to support choice of the factor 0.8, there was little sympathy elsewhere for it. Its conservatism was questioned by Rosen (Testimony), Lawson (Transcript 5755), and Ybarrondo (Transcript 6076, 6270, 10255), and its continued use has not been proposed by the Staff.

It appears that consideration of flow redistribution prior to the hearing comprised only circumstances in which the clad is not deformed. Questioning during the hearing also dealt with effects of clad swelling, fuel deformation, and partial blockage on flow redistribution. It was less apparent that the factor 0.8 would be adequate if partial channel blockage occurred than if channels were undeformed.

The Staff's Supplementary Testimony recognized this point, and proposed that models be developed and used that explicitly calculate the effect of flow redistribution during both the upflow and downflow phases of blowdown. The view included use of models that calculate the flow redistribution resulting from flow blockage if that should be calculated to take place.

We believe this is the correct course to follow. We believe the wording in the Staff's proposed rule adequately expressed the position supported by the record, with one exception. There is no basis in the record for continued use of the flow reduction factor of 0.8 after flow redistribution effects have been calculated for the hot channel. We have not included this requirement in the Rule (60, p. 1120).

With respect to reflooding, core blockage is not expected to produce a significant reduction in reflood rates. A uniform core blockage of 75 percent was calculated to produce a pressure drop increment of "no greater than 0.3 psi." The resulting change in the reflood rate was calculated to be "less than 3 percent" (4, p. 20-5).

However, the relatively small overall effect on reflood rates does not necessarily indicate that local effects due to flow diversion from hot spots may not be significant. W. R. Gambrill of ORNL was reported to have made calculations which "indicated that clad temperature increases between 500°F and 2000°F" could occur as a consequence of blockage (4, p. 20-8).

Although FLECHT experimental results must be recognized as poorly simulating the open lattice core of the PWR, it should be noted that they have been frequently cited as indicating that core blockage may actually improve local HTC with consequent reductions on local temperatures. Tests of 10 x 10 rod arrays with up to 100 percent central blockage in a field of 75 percent blockage (using flat plates to achieve midplane orifice channel blockage) showed reductions in midplane temperatures of about 200-250°F as a result of blockage. (The unblocked temperature increase during reflood was 465°F; restricted flow temperature increases, 193-245°F.)

Westinghouse has observed the evident limitations on FLECHT simulation of flow diversion. They have stated:

It should be noted that no attempt was made to simulate core wide radial flow effects in the PWR FLECHT tests. Typical reactor loss-of-coolant accident calculations indicate that the coolant flow at the midplane of the "hot" assembly with 50% flow blockage would be approximately 75% of the core average. Therefore it is important to recognize the need to take the radial flow distribution into account in using FLECHT data for reactor loss-of-coolant accident analyses (50, p. 4-5).

As a consequence of the lack of apparent correlation between experiments and analyses, the effect of potential PWR core blockage on reflood heat transfer is uncertain. As the AEC has observed:

Vendor estimates of the effects of flow blockage on peak cladding temperature range from 76° (CE) to 90°F (Westinghouse) to 159°F (B&W). Suggestions for an allowance to be applied to account for blockage range from 100°F (Gambrell, ORNL) to "more appropriate power/flow ratios" (BNWL)* to general agreement with the Regulatory Staff discussion of this section (ANC) to a 500°F penalty (Colmar) (4, p. 20-18).

The regulatory staff acknowledges that their overall review "suggests that there could be a temperature increase due to blockage as a result of degraded heat transfer (extended period of low steam flow) at early

* Battelle Northwest Laboratories

times..." Moreover, with low flow, calculations "indicate a flow reduction could occur for specific blockage configurations" (4, p. 20-18).

Flow blockage effects in a BWR are somewhat less of a problem than in a PWR. BWR fuel rod internal gas pressure differences during a LOCA are expected to be less than 55 psi when compared with pressures in the reactor core. Test data indicate that rupture of a rod at these pressure differences requires temperatures above 2200°F (4, p. 20-19). Although, as previously discussed, a large amount of individual rod swelling may take place, swelling of the outer rods next to the cooler channel walls is unlikely and no rod swelling is expected during blow-down. Though ballooning of interior rods in the 7 x 7 or 8 x 8 individual rod bundles may take place, they are expected to rupture towards the relatively hot center of the bundle. At least 90 percent overall assembly blockage is required before bottom flooding is impaired, the AEC has reported (4, p. 20-21). Since the area between the outer rods and bounding metal channel for each bundle is nearly 30 percent of total fuel bundle cross-sectional flow area, ballooning of the inner rods is not expected to affect core flooding capability. Moreover, each individual 7 x 7 or 8 x 8 BWR fuel bundle is a self-contained unit with its own separate zircaloy channel bounding the bundle. Consequently, radial cross-flow out of the bundle (away from the hot spot) is restricted. The AEC has estimated that BWR flow blockage will induce peak temperature increases of less than 60°F. However, extreme estimates, with complete restriction of radiation view factors to other than adjacent rods and a zero convective HTC for interior rods, have indicated peak clad temperatures might increase 520°F before termination of the temperature transient (4, p. 20-22). In spite of this extreme example, flow blockage does not appear to be as serious a problem for the BWR as it does for PWR.

A9.4.4 Steam generator tube failure effects

One of the primary concerns of the CNI with respect to treatment of loop resistances in fluid flow through the PWR reactor heat

transfer loops is related to the problem of PWR steam generator tube failures. In the event of failure of a tube in the steam generator during the LOCA, the high pressure-high temperature fluid in the secondary steam system providing steam to the power plants' turbine-generator could potentially feed back large quantities of steam into the primary loop of the reactor steam system. The concern of CNI is that the steam added to the primary system from the large secondary loop supply could effectively choke the flow of the escaping gases in the system during reflood, reducing reflood rates below their already precariously, hazardously low values.

The CNI concern is perhaps best summarized by these statements:

Steam generator tube failures in a PWR give every appearance of being the Achilles heel of PWR ECCS. Dr. Morris Rosen and Mr. Robert Colmar in their June 1, 1971 memo [to the ECCS Task Force] wrote:

"Although arguments are being made that we can conservatively predict performance during the reflooding period, recent experience indicates that problems in this area are first coming to light, e.g., plugging of lines and locations of safety injection systems. Notwithstanding all the assurances that one can obtain upper bounds on two-phase and superheated steam pressure drops, very little is known about these types of flow at high velocity through turning pipes, steam generators and pumps.

Of paramount concern in this area, however, is the possible effect of steam generator tube failures on the ECCS. We have been told that as few as [Censored] steam generator tube failures could prevent reflooding. It seems clear that the area of steam generator integrity during blowdown requires an immediate and thorough evaluation."

The Regulatory Staff apparently treated the Rosen-Colmar concern in a quite superficial manner. Thus, Dr. Hanauer testified that there were some general discussions on the subject within the Regulatory Staff, but that no one was assigned to study the question (TR. 2335)(7, p. 3.22).

In their concluding arguments the CNI summarized their concern as:

(6) Steam Binding: Steam binding is the name given to the counter-active force from steam generation in PWR reflood that acts to defeat or reduce the emergency coolant reflood rate during injection and vaporization of emergency coolant. It is the phenomenon recognized late in the reactor program which is the source of the great reduction in reflooding rates. Steam binding can be greatly aggravated by steam injected as a result of steam generator tube failures.

(7) Steam Generator Tube Rupture: It has been established that the rupture or failure of only a very few, a handful, of the steam generator tubes in a PWR under LOCA conditions can inject sufficient steam to stall totally the reflood capability and so to insure clad melting. It appears likely, if not certain, in view of the known aggravated corrosion of and wall thinning in steam generator tubes in several operating reactors, that the forces developed by a cold-leg pipe break would rupture sufficient tubes to cause this stalling. The matter is discussed elsewhere in our findings. The subject, although of obvious importance, was not considered in this proceeding (7, pp. 5.48, 5.49).

Some added quantitative feel for the magnitude of the problem is given by Brockett, et al. They described the steam generator tube leakage problem as follows:

During core reflooding the fluid from the secondary side of the steam generator has the potential to transfer energy, and in the event of a tube leakage also mass, to the primary system. Transfer of either mass or energy can add significantly to the pressure drop from the upper plenum to the downcomer annulus. Unless the secondary side has depressurized before reflooding is initiated, significant errors in reflooding rates would occur if energy transfer processes are not properly taken into account. Current predictions, which are based on the secondary side of the steam generator being at normal operating temperatures at reflooding initiation, are considered to account for all energy transfer processes. If only a few tubes leak, mass transfer processes can also significantly add to the error in the calculated reflooding rates. To provide a 17% reduction in reflooding

rates (at 1.5 in/sec) a tube break area of about 0.003 ft² would be required. Present predictions are based on the assumptions that none of the tubes fail (10, p. 320) (emphasis added).

In rebuttal, the AEC has simply sidestepped the issue, stating in their Concluding Statement:

5. Steam generator tube failures.*
6. Pressure vessel failures.**
7. Fuel densification.***

In its Order of February 23, 1972 the Commission observed with respect to scope rulings that 'the technology and the issues may not present clear-cut answers to questions of inclusion [of matters in the hearing record].' Some cases are clearer than others, however. Thus, where the integrity of steam generator tubes and reactor pressure vessels are covered by other Commission regulations,**** such matters are properly excluded from the scope of the present proceeding which is concerned with emergency core cooling systems' ability to control the consequences of a large pipe rupture, not a break in a pressure vessel or cracks in steam generator tubes, which problems are dealt with elsewhere.

Similarly, the question of fuel densification--a phenomenon which came to light wholly outside the ECCS hearing but while it was still going on--is treated on an ad hoc, case-by-case basis. The staff chapter on this subject in its Supplemental Testimony--included for information purposes--made just this point and was stricken on that basis as beyond the scope of the proceeding. In all events, since the question is not within the scope of this rule making, it is properly a subject for consideration and has been so considered in individual cases that have arisen.

*	CNI Statement, pages 2.7, 3.1
**	CNI Statement. pages 2.8, 3.1
***	CNI Statement, pages 2.8, 3.26-3.41
****	10 CFR Part 50 Appendix A, General Design Criteria (6, p. 16).

In an earlier portion of the AEC Concluding Statement they amplified on the concept of giving out-of-scope subjects further subsequent consideration. They stated specifically:

An issue or subject ruled beyond the scope of this rulemaking proceeding is not proper within the scope of any rule that ultimately eventuates. Thus, rulings that have the effect of narrowing or expanding the scope of the rule making merely result in a narrowing or expansion of the potential coverage of any ultimate rule. And the breadth of the rule -- be it narrow or broad -- cannot be the source of "prejudice" to anyone, since matters ruled within the scope of the proceeding are proper for consideration here, while matters ruled beyond the scope of the present rule making are subject to consideration either in individual licensing proceedings or in the context of a petition for further rule making (6, p. 13).

While the subject of the contribution of steam generator tube failures to steam binding and fluid flow restriction within the primary reactor coolant loop may have been deemed beyond the scope of the present hearings, it appears unlikely that it will be the last time the AEC will be required to deal with this problem. Though the information developed in the hearings with respect to the problem is inadequate to permit a reasonable evaluation of the subject, as a result of the AEC ruling, the problem appears to be of sufficient magnitude that a ruling will ultimately have to be made on it.

A9.5 Summary

In analyzing the subjects reviewed in this appendix, including analytical models and numerical methods; treatment of break flows; transient critical heat flux and heat transfer; reflood rates and heat transfer; and effects associated with flow blockage, it seems that the ACRS concern that the preceding representative items "are considered not proven to be conservative" (19) is still justified. The regulatory staff made serious attempts to demonstrate that adequate conservatism had been shown for each of the items of ACRS concern (AEC Concluding Statement, 6). Nevertheless, it appears that as far as the above items are concerned, there are still unresolved questions of conservatism. This judgement should not be considered to imply that nothing is known about the subject items. On the contrary, in most cases a great deal

of information has been gathered. Generally speaking, however, additional testing accompanied by appropriate analysis is still required to develop confidence that models of all items and their treatment under the revised acceptance criteria are adequately conservative.

The problems associated with reflood heat transfer are especially critical. If current estimates of low flooding rates (of the order of 1 to 2 in/sec) remain valid, blockage induced flow reduction to the hot channel could be a very serious problem. Under these conditions, the uncertainties associated with heat transfer coefficients, flooding rates and flow distribution make core coolability during a LOCA uncertain. More conservative estimates of critical parameters in these areas would certainly raise predicted peak local temperatures, perhaps in excess of 500°F.

In the revised AC, the Commission attempted to introduce more conservative restrictions on methods of dealing with low flooding rates and their associated problems. As examples of requirements intended to increase conservatism, the AC requires: the prediction of swelling and rupture for cladding whenever they occur during the course of the LOCA; and the assumption that PWR cooling takes place by steam only, if reflood rates are less than one inch per second (taking into account any blockage effects on steam flow induced by clad swelling and rupture). Though the assumption of steam cooling for the flow rates indicated was intended to increase conservatism, there is some question about whether it is a viable requirement. James O. Zane, Manager of Experimental Projects for the Aerojet Nuclear Company of Idaho Falls testified at the hearings before the JCAE (22-24 January 1974) concerning the questionable conservatism of this assumption:

Mr. Zane. I am trying to make my point that the new rule is likely to have some problem. I think in this particular area, flooding rates below one inch per second have been a concern because of the limitations of FLECHT data. Someone has specified that we will make a calculation in the future

assuming only steam cooling. My point here is that if you were to do that you would find yourself in a very inconsistent situation. The codes I think may exist today for doing this, but in a previous section of the rules dealing with reflooding rate it has been specifically stated they will not be used, you will simply use experimental data.

It seems to me to be a contradiction or conflict here in sections of it. I use this as an example to point out that I think as our regulatory and the applicants live with some of these decisions that they are going to have minor problems working out some of the technical aspects of this.

Chairman Price. Are you suggesting that the Commission change its position?

Mr. Zane. In this particular case I would suggest that the Commission would take a little different approach. I think in other areas a similar thing. If I were to take a position on this particular matter, instead of the rule as it is written I might simply say that for reflooding rates less than one inch per second that additional safety systems -- I wouldn't accept a reflooding rate below one inch per second if I were in this situation.

Chairman Price. Which position is more conservative, your position or the Commission's opinion?

Mr. Zane. Mine would be. Here they are going to give them some credit but they are suggesting that they calculate it in a way that seems to me is not within the state of the art at the moment to do a very reasonable calculation (59).

It seems apparent that the strong implication of all of the above is that a criteria specification should be made for higher flood rates (as high as 6 in/sec or greater), or equivalently higher reflood heat transfer coefficients (in excess of 40 B/hr-ft²-°F). These values seem large enough to achieve desired levels of conservatism. If implemented, reflood would achieve not only rapid temperature turnaround, but rapid rod quenching as well. Currently it appears that only a narrow margin of coolability, if any, exists. In light of uncertainties in ECCS performance parameters, cooling by only narrow margins appears unacceptable. In fact, the Commission itself has stated (in the Opinion to the AC) that:

The Commission believes that the calculated reflood rate should have a substantial margin over the rate which is just sufficient to turn the temperature around in a short time....Steam binding would severely limit the rate of reflooding the core, reducing it from an intended 6 to 11 inches per second to from 1.0 to 2.5 inches per second, depending on reactor design....The Commission urges the pressurized water reactor manufacturers to seek out design changes that would overcome steam binding (60, p. 1092) (emphasis added).

As discussed above, such low reflood rates do not appear to represent the "substantial margin" required to assure adequate cooling. The higher flooding rates proposed as a criterion (approximately 6 in/sec) would appear to be adequate to overcome the current narrow margins and provide acceptable levels of conservatism in reflood heat transfer. The author strongly supports the Commission in urging reactor manufacturers to "seek out design changes that would overcome steam binding" which could provide the higher recommended reflooding rates.

Appendix 10 RELATIVE IMPORTANCE OF PARAMETERS AFFECTING THERMAL RESPONSE

Many statements have been made by the AEC and vendors concerning conservatism, or even excessive conservatism, of the IAC and the PR. These statements are based upon a number of "realistic" LOCA analyses with associated estimates of model parameter importance which have been made by vendors and the AEC. This section will summarize and attempt to put into perspective some of these analyses and their implications with respect to the relative importance of the parameters investigated.

A10.1 Vendor Conceptions of Model Conservatism

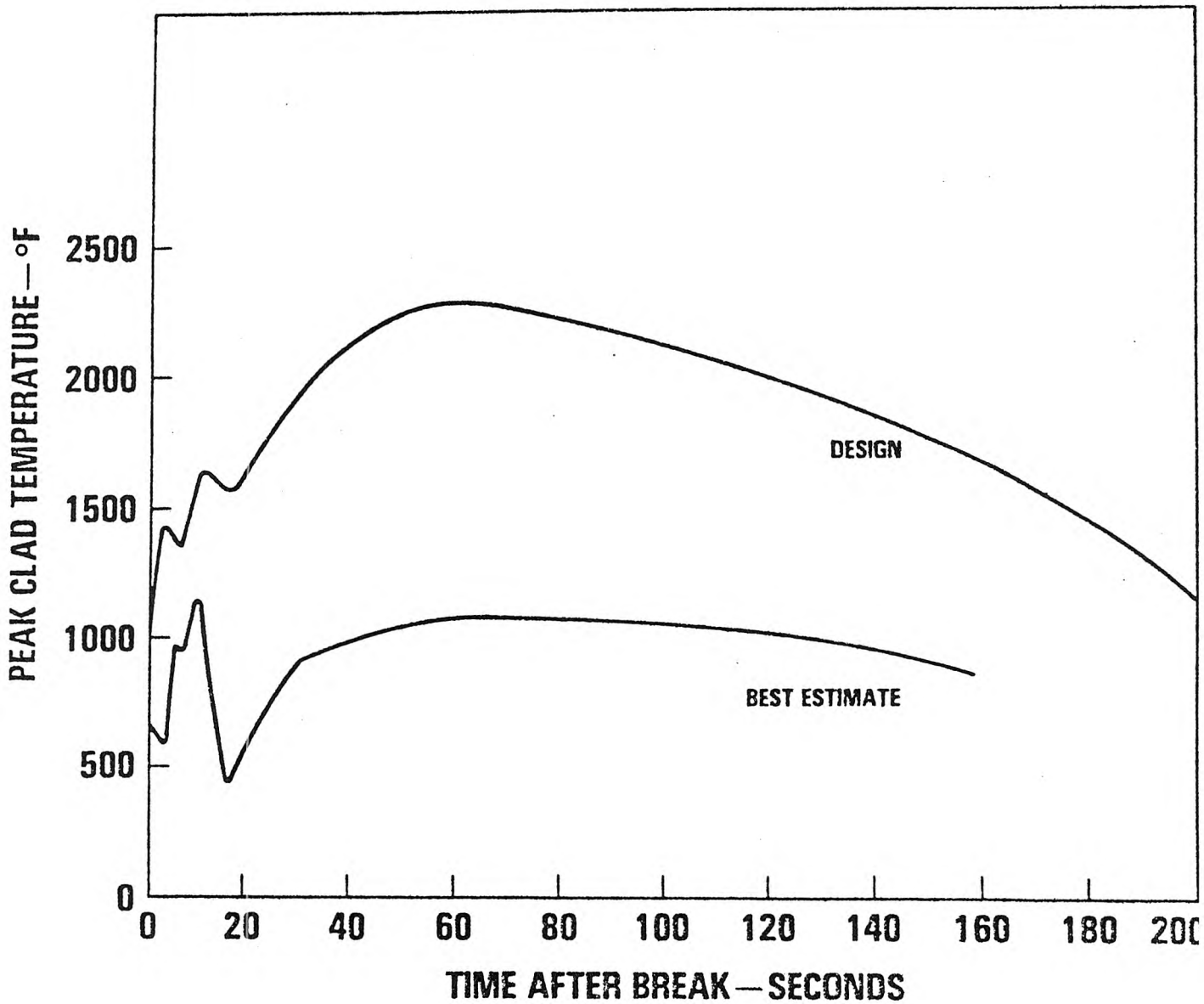
A number of examples of vendor "realistic" or "best estimate" calculations of LOCA thermal response could be cited. Two typical graphical examples of comparisons between analyses conducted in accordance with IAC requirements and those made with assumptions felt to be "more realistic" by the vendors are shown in figures A10.1 and A10.2. Figure A10.1 (reproduced from 31) shows the comparison of a Westinghouse "best estimate" calculation with the IAC-imposed design method calculation. Note that the peak temperature calculated under the IAC, designated "design" in the figure, approaches the 2300°F limit, and is attained during reflood. In the "best estimate" calculation, the peak temperature is only about 1200°F, approximately one-half the 2300°F IAC limit, and occurs during blowdown.

Figure A10.2 (reproduced from 32) presents a similar comparison of a General Electric prediction with the analysis based upon IAC requirements. Note that the peak temperature of the GE calculation (~1800°F), even under IAC assumptions, falls substantially short of the IAC 2300°F limit. With the "realistic" prediction, the maximum temperature occurs during blowdown and does not exceed 800°F.

Before considering the assumptions used in such "realistic" calculations, it is well to consider the comments of the AEC with respect

Figure A10.1
Westinghouse Comparison of "Best Estimate" and IAC
Design Requirement LOCA Calculations

DOUBLE ENDED COLD LEG BREAK (GUILLOTINE)



(After Figure 7, 31.)

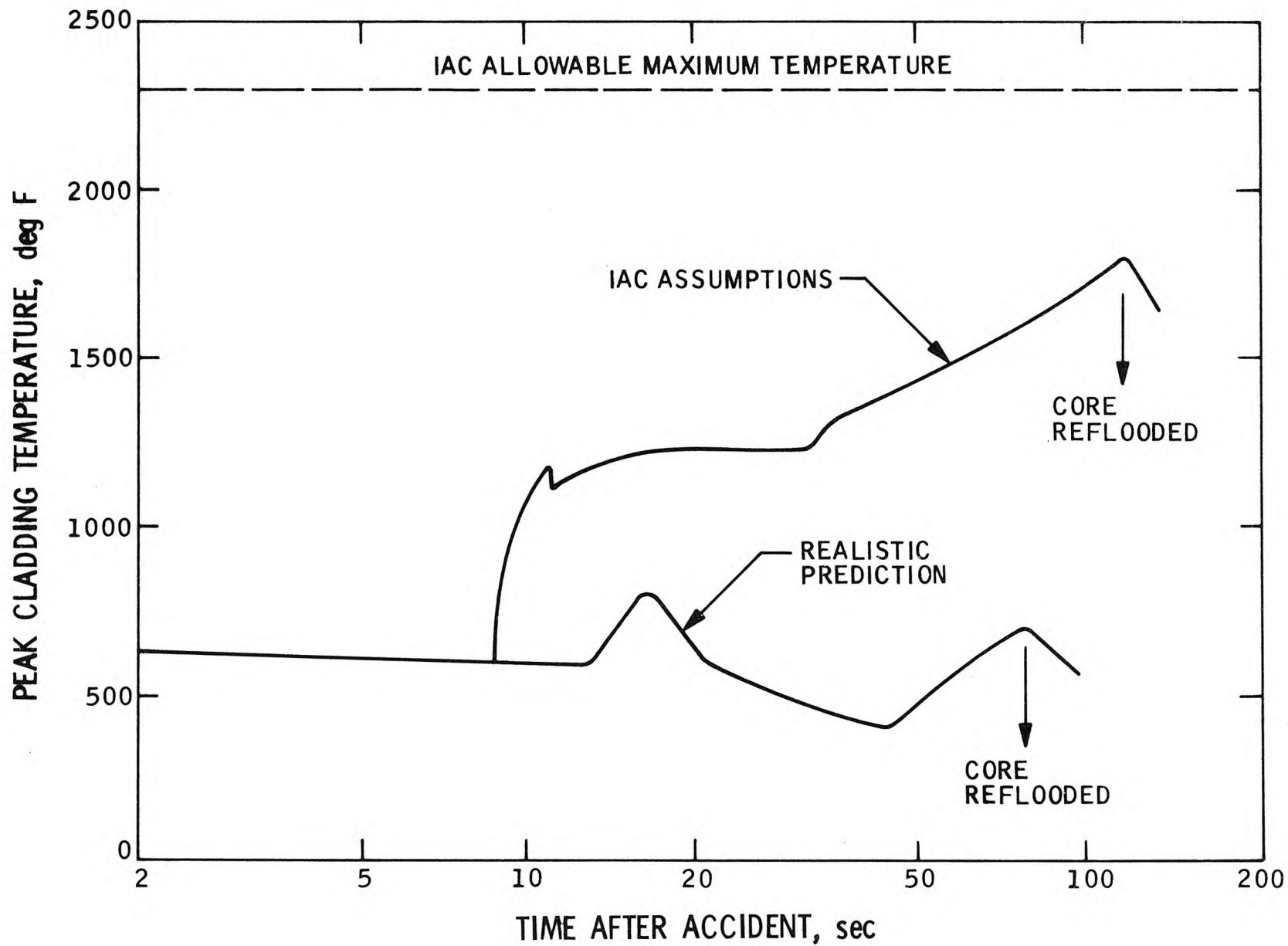


Figure A10.2 Basis and Comparison of GE "Realistic" and IAC Constrained Calculations of LOCA Thermal Response
(After Figure 15, 32, by permission.)

to them. In the AEC Supplemental Testimony, they commented:

The vendors in their direct testimony reported the results of "expected" or "realistic" analyses of the design-basis LOCA for their respective reactor designs. They claimed that these calculations provide an estimate of the conservatism of the analysis methods prescribed in the Interim Policy Statement. These are not the first calculations which estimate that conservatism; sensitivity studies have previously been performed (see Regulatory staff direct testimony) which establish the influence of certain LOCA parameters.

A shortcoming of the vendors' "realistic" calculation is that they are not "best estimate" in a statistical sense. In the strict statistical sense, all input parameters and their associated uncertainties would be taken into consideration in performing best estimate calculations. Such analyses would account for propagation of uncertainties and would provide confidence bands for the final calculated results. The Regulatory staff is developing methods at ANC to perform such calculations. Several vendors have also indicated analytical development efforts in this area (TR. 15,432 and 14,432) (4, p. 2.1).

In fact, the actual statistical basis is very weak for the "realistic" or "best estimate" calculations. The experimental basis for statistically accurate estimates of many of the LOCA phenomena is weak or non-existent (especially for overall integrated system operational results). Consequently, most "realistic" calculations (even those having an apparent statistical formulation) depend heavily upon "engineering judgement" for parameter probability specification in lieu of an adequate empirical base for a statistical analysis.

It should also be observed that the presentations in figures A10.1 and A10.2 have a pronounced bias towards an "optimistic" picture of ECCS control of a LOCA. That is, in estimating critical thermal parameters for the "best estimate" calculation, the "optimistic" side of "confidence bands" for the ranges of uncertainty for the parameters has been chosen. The discussions of parameter uncertainties in chapter 3 attempted to express the alternative view, i.e., the "worst case" or

pessimistic side of the confidence bands for parameter uncertainty. The vendors have based their "best estimate" calculations on the observation that the IAC parameter requirements are biased heavily (in their opinions) towards the "pessimistic", or conservative, view of parameter uncertainty. However, the AEC's comment that vendor calculations do not "account for uncertainties" or "provide confidence bands for the final calculated results" (4, p. 2.1) is indicative of a more objective view of these so-called best estimate calculations. If the calculations followed the AEC recommendations, and limiting values for confidence bands of parameter uncertainty were utilized for both optimistic and pessimistic views of the parameters, then the optimistic side of the results would probably look much like the vendor presentations while the pessimistic view might more nearly reflect the LOCA thermal history for conditions portrayed by the analysis of chapter 3.

An example of the basis for decisions concerning parameters for which "unrealistic" assumptions are considered to be required in the IAC is given by GE in table A10.1 (reproduced from 32). This table lists the parameters for which GE feels that the IAC imposes assumptions which are "excessively conservative". Figures A10.3 and A10.4 present a graphic comparison of the heat transfer coefficients (HTC) associated with the LOCA calculations based upon IAC requirements and "realistic predictions" respectively. The HTCs assumed in figure A10.4 were used to calculate the thermal response of figure A10.2. Table A10.2 (27, sec. G) shows the results of separately (and individually) varying selected parameters from table A10.1 to values felt to be more "realistic" by GE. Note that in the results presented in table A10.2 the durations of critical periods for parameters and assumed HTCs associated with them are probably similar to those shown in figure A10.4. Comparing the assumptions of figure A10.3 and A10.4 shows that the assumed rod rewetting (figure A10.4) during low plenum flashing results in much higher HTCs (by a factor of approximately 100) than the IAC allows for the same

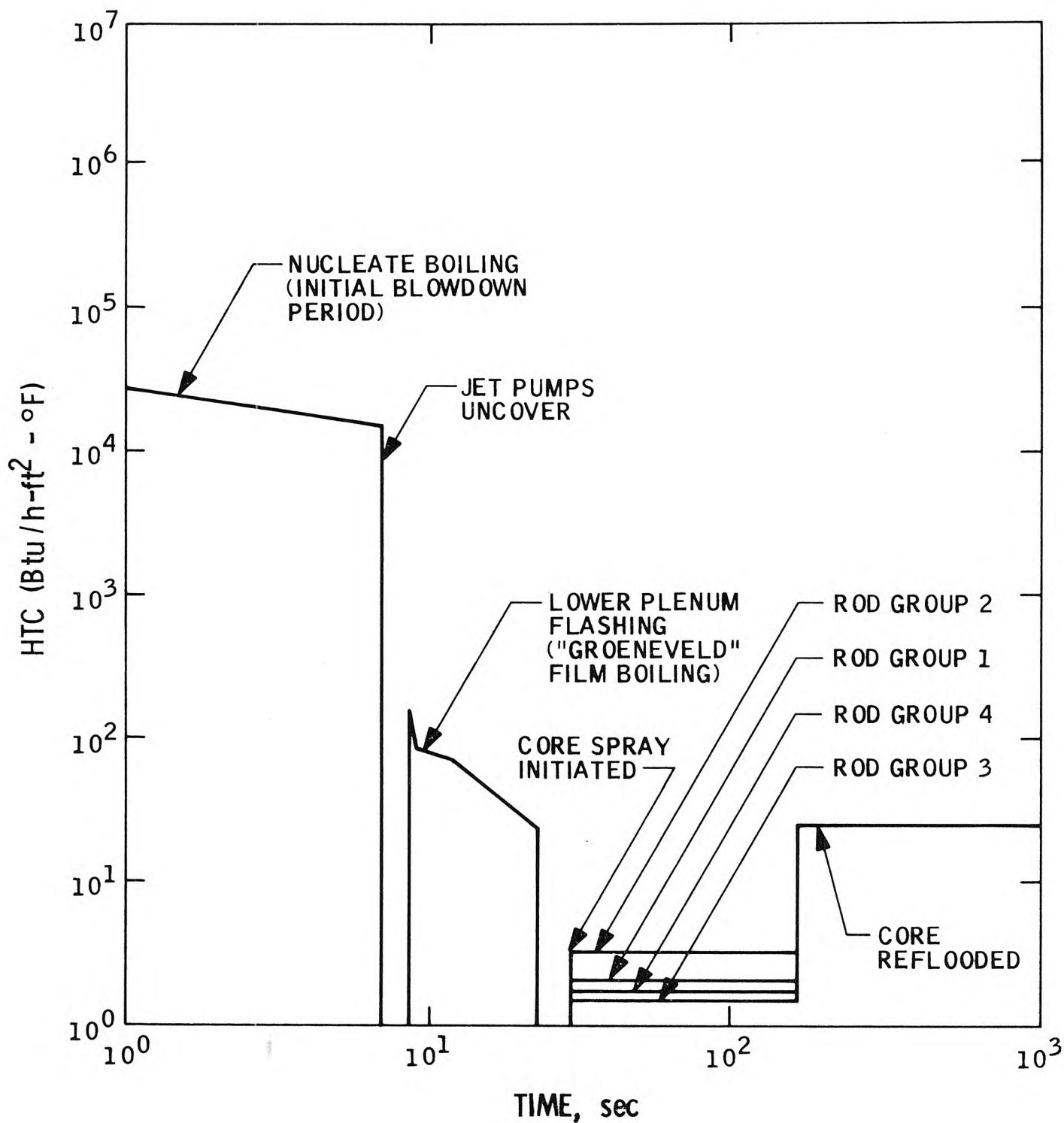


Figure A10.3 Heat Transfer Coefficients for Design-Basis BWR LOCA (Interim Criteria Assumptions Used)

(After Figure F-3, 27, by permission.)

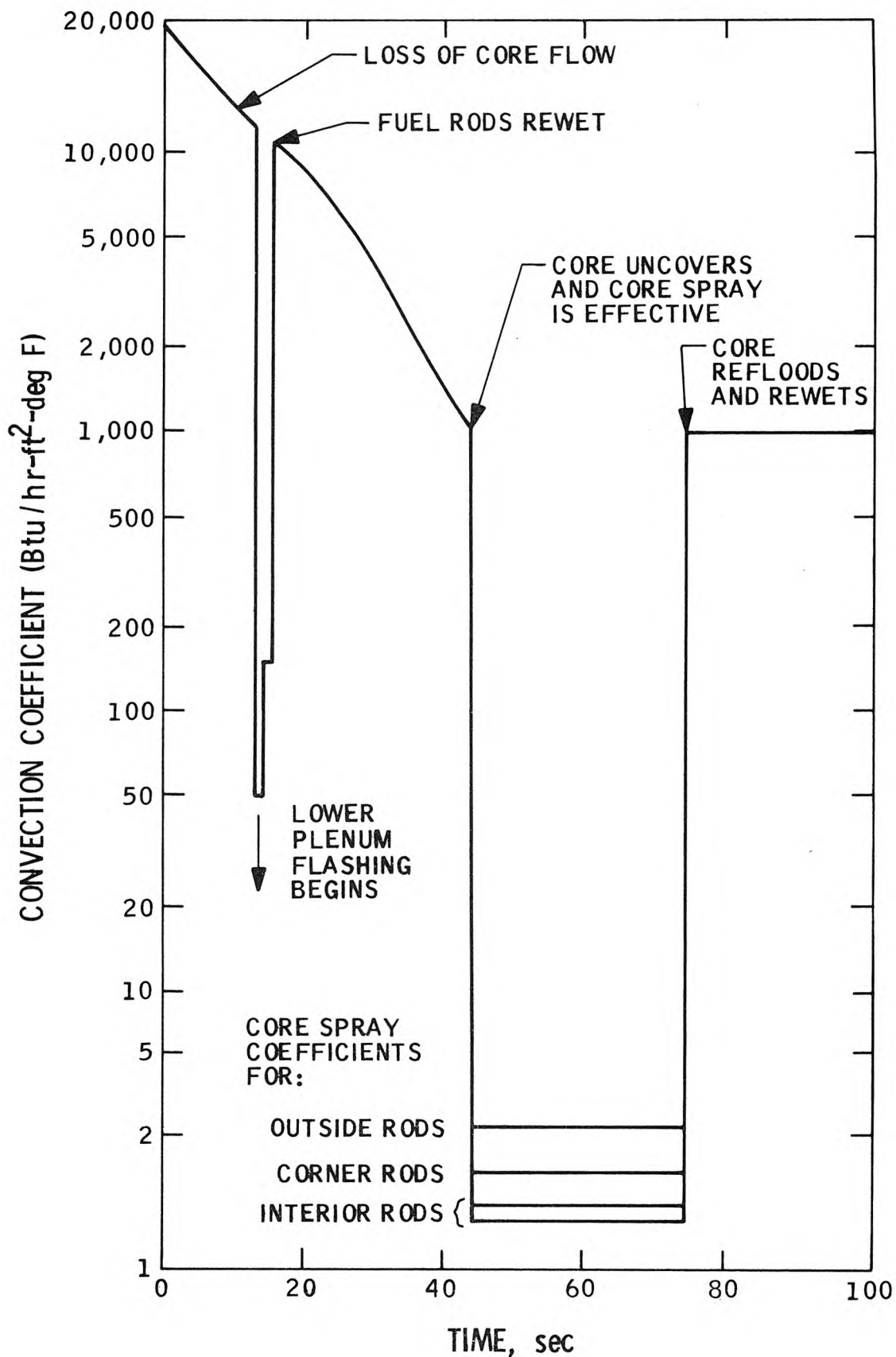


Figure A10.4 Rod Heat Transfer Coefficients for GE "Realistic" Prediction of LOCA Thermal Response
(After Figure 14, 32, by permission.)

Table A10.1

General Electric Listing of IAC Required "Conservative" Assumptions
 CONSERVATIVE ASSUMPTIONS REQUIRED FOR BWR LOCA LICENSING EVALUATION

<u>Parameter</u>	<u>Required Assumption</u>	<u>Effects of Assumption on Accident Analysis</u>
<u>BLOWDOWN PHASE</u>		
1 Break critical flowrate	Use Moody Model	Maximizes flowrate and break severity
2 Feedwater flow	Ignore feedwater	Does not account for feedwater replacing water lost through break
3 Duration of nucleate boiling heat transfer	Assumed nucleate boiling continues only until jet pumps uncover	Minimizes removal of stored energy during blowdown
4 Heat transfer during lower plenum flashing	Use Groeneveld film boiling equation (2,4)	Minimizes removal of stored energy during blowdown
<u>CORE HEATUP PHASE</u>		
5 Fuel assembly axial and radial power factor	Use maximum design values	Maximizes stored energy and heatup rate
6 Local power distribution	Use worst case operating peaking	Does not account for power flattening due to gamma redistribution
<u>EMERGENCY CORE COOLING PHASE</u>		
8 System initiation signal	Use latest occurring signal	Delays ECC initiation
9 Normal auxiliary power	Assume not available	Delays ECC for diesel generator starting
10 Containment backpressure	Ignore backpressure	Reduces effective ECC pump head
11 Cooling system operable	Use systems available after worst single failure	Reduces total ECC flowrate, delays core reflooding
12 Time when fuel bundle channels are wetted	Use Yamanouchi analysis plus 60 seconds	Delays radiation to channel cooling

Sensitivity of Peak Clad Temperature During
Core Spray Operation to Specified Variables

Item from Appendix III of Exhibit 1069	Decrease in Peak Clad Temperature (°F) From Base Case	Discussion
(3) Duration of Nucleate Boiling	Approx. 120	A previous sensitivity study on this item showed that the peak clad temperature decreased 40°F for each second of delay of CHF. It is more important to note that for the expected duration of nucleate boiling the rods will rewet.
(3) Heat Transfer During Lower Plenum Flashing	Approx. 800	Rewetting of the rods would decrease the cladding temperature to nearly saturation temperature by the time the fuel uncovers. The peak cladding temperature during core spray operation in this case is merely the heatup during the time of fuel uncover.* This item renders insignificant all items that pertain to the portion of the accident prior to uncover.
(5) Critical Flow Rate for Liquid	Approx. 300	The sensitivity of this item has been investigated using the models of NEDO-10329. Changing the critical flow rate essentially changes the time-scale of blowdown without affecting the decay power significantly. Essentially the same rate of energy removal results with a decreased lower plenum flashing inventory loss.
(6) Cooling Systems Operable	Approx. 150	Analyses have been made using the models of NEDO-10329 for the case in which all cooling systems are operable, as well as for cases with single failure of an active component.

*The peak clad temperature over the entire transient for this case is about 1300°F, and occurs at the start of lower plenum flashing. Since core heatup after lower plenum flashing begins at only about 500°F, the clad temperature at core reflood will be only about 1100°F. It is this latter figure--the peak clad temperature during core spray operation--that is compared with the base case in the above table.

Table A10.2

Sensitivity Analysis of Critical G.E. LOCA Assumptions for "Realistic" Evaluations of Parameters (Ref. 27)

phenomena, and the high HTC's of both the initial blowdown prior to DNB and lower plenum flashing periods are retained for much longer than the IAC allows. The net result is that, as indicated in figure A10.2, the heat transfer during lower plenum flashing more than compensates for the decay heat released during that period (from approximately 20 to 45 sec) together with the remaining stored energy of the fuel so that the temperature during this period actually is predicted to fall below normal operating temperatures. Even though the HTC's during core spray, prior to reflood, are very low, the decay heat remaining during the core spray period is calculated to be insufficient to raise the rod temperatures as high as the blowdown peak reflood occurs. Moreover, the "realistic" calculation assumes reflood occurs substantially before it is allowed under the IAC requirements. In addition, the resulting reflood HTC, with assumed rewetting of the rods in the "realistic" case, is much higher (by approximately a factor of 40) than permitted under the IAC.

Under these conditions, as described in table A10.2, the critical parameters are shown to be:

- (1) The duration of nucleate boiling (associated with CHF predictions) producing a 120°F decrease in peak clad temperature, based upon an apparent increase in time to CHF of approximately 3 seconds.
- (2) Heat transfer during lower plenum flashing (the most (overall) influential factor affecting thermal response) producing a reduction in the late time temperature of approximately 800°F. In the GE realistic picture, heat transfer during lower plenum flashing also depends heavily on the CHF correlations. The large HTC associated with rod rewetting are assumed based upon calculation of reduced rod heat flux well below CHF during the

flashing period so that rod rewetting is assumed to be assured and nucleate boiling reestablished.

- (3) The critical flow rate for liquids under the IAC was based upon the Moody model which has been previously discussed as possibly underpredicting break flow during at least a portion of the blowdown period. Analyses of experiments conducted for fairly large scale pipes are cited as evidence that predicted flow rates will be approximately 0.8 of the Moody predicted values. On this basis, the blowdown period is extended with a consequent effective increase in the duration of the period of nucleate boiling, similar to (1) above but apparently longer so that peak temperatures during core spray operation are reduced by approximately 300°F.
- (4) Failure to keep the cooling systems operable is the "worst single failure" assumption of the IAC and requires the assumption of the loss of auxiliary power at the same time the LOCA occurs. GE feels that the "high quality and high reliability" requirements imposed on ECCS design realistically prevent this type of outage from occurring. If, in opposition to IAC requirements, auxiliary power is maintained, the ECC pumps will operate on schedule and core reflooding can be accelerated. Core reflooding generally results in almost instantaneous temperature turnaround. Earlier functioning was found to reduce peak temperatures by 150°F. It is interesting to observe that of all the dominant parameters, only this one is significant in the period following core dryout. All other critical parameters are associated with blowdown.

Figure A10.5 (27, sec. G) shows the GE assignment of the probabilistic interrelationships between branch elements of several of the critical LOCA paths. Probabilities of event occurrence have been assigned to each branch of the LOCA event tree by the GE authors. On the basis of the assigned branch probabilities, the joint probability of each branch could be calculated. Within this framework, the LOCA thermal response corresponding to the combination of events for any branch can be calculated and the probability associated with the corresponding peak temperature "derived" from the joint probability of the branch.

Calculations of the peak temperatures associated with the several branches were made and a curve, figure A10.6, of the probability of achieving any given maximum temperature was obtained from the probabilistic relationships of figure A10.5. In the description of the authors:

It would be presumptuous to suggest that all the realistic assumptions are without significant levels of uncertainty. In fact, the uncertainties in the various assumptions as well as in the cladding temperature prediction have been assessed [as shown in figure A10.5] ...Figure 16 [A10.6] shows the final result in the form of a complementary cumulative distribution function of peak cladding temperature. Note that the realistic prediction of 800°F is actually a best estimate or most probable result and there is an equal probability that temperatures will be higher or lower than this value. The probabilities in Figure 16 [A10.6] do not include the probability of occurrence of a LOCA. Thus, for example, the figure indicates that given one hundred BWR LOCAs only 1 of these would result in a cladding temperature exceeding 1200°F. The real importance of this result is in the illustration that even with significant levels of uncertainty on individual parameters, the probability that many of the parameters will combine in a worst case manner is very low. In any event, it is clear that temperatures predicted with the IAC evaluation assumptions applied to the BWR represent a highly unlikely outer bound (32).

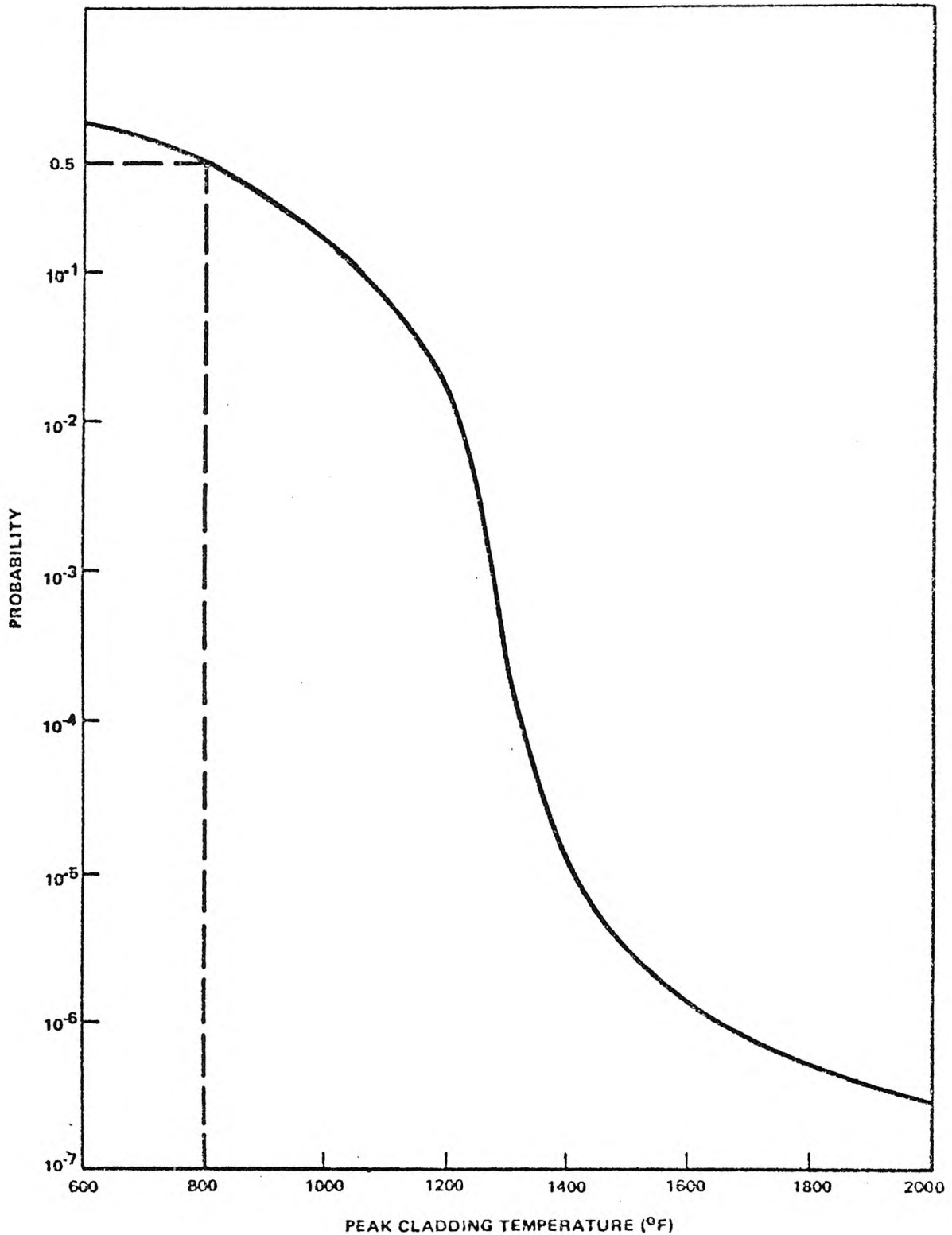
PARAMETER			
BREAK CRITICAL FLOW RATE	DURATION OF NUCLEATE BOILING	HEAT TRANSFER COEFFICIENT DURING LOWER PLENUM FLASHING	DECAY POWER GENERATION
VALUES			
			BE + 2σ (0.15)
		$h_M - 2σ$ (0.15)	BE (0.7)
	JPU + M - 2σ (0.15)	h_M (0.7)	BE - 2σ (0.15)
1.0 MM (0.15)	JPU + M (0.7)	$h_M + 2σ$ (0.15)	(SAME AS ABOVE)
	JPU + M + 2σ (0.15)	(SAME AS ABOVE)	(SAME AS ABOVE)
	JPU + M - 2σ (0.15)	(SAME AS ABOVE)	(SAME AS ABOVE)
0.8 MM (0.7)	JPU + M (0.7)	h_M (0.3)	(SAME AS ABOVE)
		REWET (0.7)	(SAME AS ABOVE)
	JPU + M + 2σ (0.15)	h_M (0.1)	(SAME AS ABOVE)
		REWET (0.9)	(SAME AS ABOVE)
	JPU + M (0.2)	h_M (0.05)	(SAME AS ABOVE)
0.6 MM (0.15)		REWET (0.95)	(SAME AS ABOVE)
	CONTINUOUS NUCLEATE BOILING DURING BLOWDOWN (0.8)		(SAME AS ABOVE)
ABBREVIATIONS			
MM = MOODY MODEL(7)	JPU = TIME OF JET PUMP UNCOVERY M = MEAN TIME TO DRY OUT AFTER LOSS OF CORE FLOW 2σ = VARIABLE (1 TO 2 sec)	h_M = MEAN VALUE OF I-II M BOILING HEAT TRANSFER COEFFICIENT (150 B/hr-ft ² F) 2σ = 50 B/hr-ft ² F)	BE = BEST ESTIMATE OF DECAY HEAT 2σ = APPROXI- MATELY 10% OF BEST ESTIMATE

(VALUES IN
PARENTHESES
INDICATE
PROBABILITY
OF BRANCH)

Figure A10.5 GE Estimates of Probability Distribution of Parameters
(From 27 by permission.)

Figure A10.6

Probability Distribution for Peak Cladding Temperature
(BWR)



PROBABILITY THAT PEAK CLADDING TEMPERATURE EXCEEDS T FOR A
BWR LOCA, BASED ON REALISTIC-ESTIMATE PARAMETERS

(After Figure G-2, 27, by permission)

Though the analysis behind figures A10.5 and A10.6 is certainly a valuable and enlightening exercise, a word of caution should be given in connection with the conclusions presented above. Figure A10.5 has the appearance of giving quantitative probabilistic estimates of the events on each of the branches. Although it has evidently been worked out with substantial thought and care, the authors have made the following comment about the values associated with the events.

The probabilities [figure A10.5] assigned to the various paths of the tree were derived considering the interactions of the variables. Thus, for example, the probability that rewetting will occur increases as the break flow rate decreases and as the duration of nucleate boiling increases. In those cases where specific data could not be applied to establish the probabilities, subjective probabilities were established based on expert technical judgement (27, p. G-4) (emphasis added).

In this probabilistic discussion, it is well to bear in mind that the probability is high that most of the specific values assigned to events of figure A10.5 were "based on expert technical judgement." If this is true, then it must be recognized that the curve of figure A10.6, though interesting and qualitatively informative, may be quantitatively fictional. These qualifiers have not been intended to negate the value of the material in figures A10.5 and A10.6. On the contrary, the results indicated by the figures should be recognized as a valuable contribution towards a preliminary estimate of the probabilistic distribution of LOCA events in terms of the maximum temperatures which might occur from them.

10.1.1 ANC parametric investigation

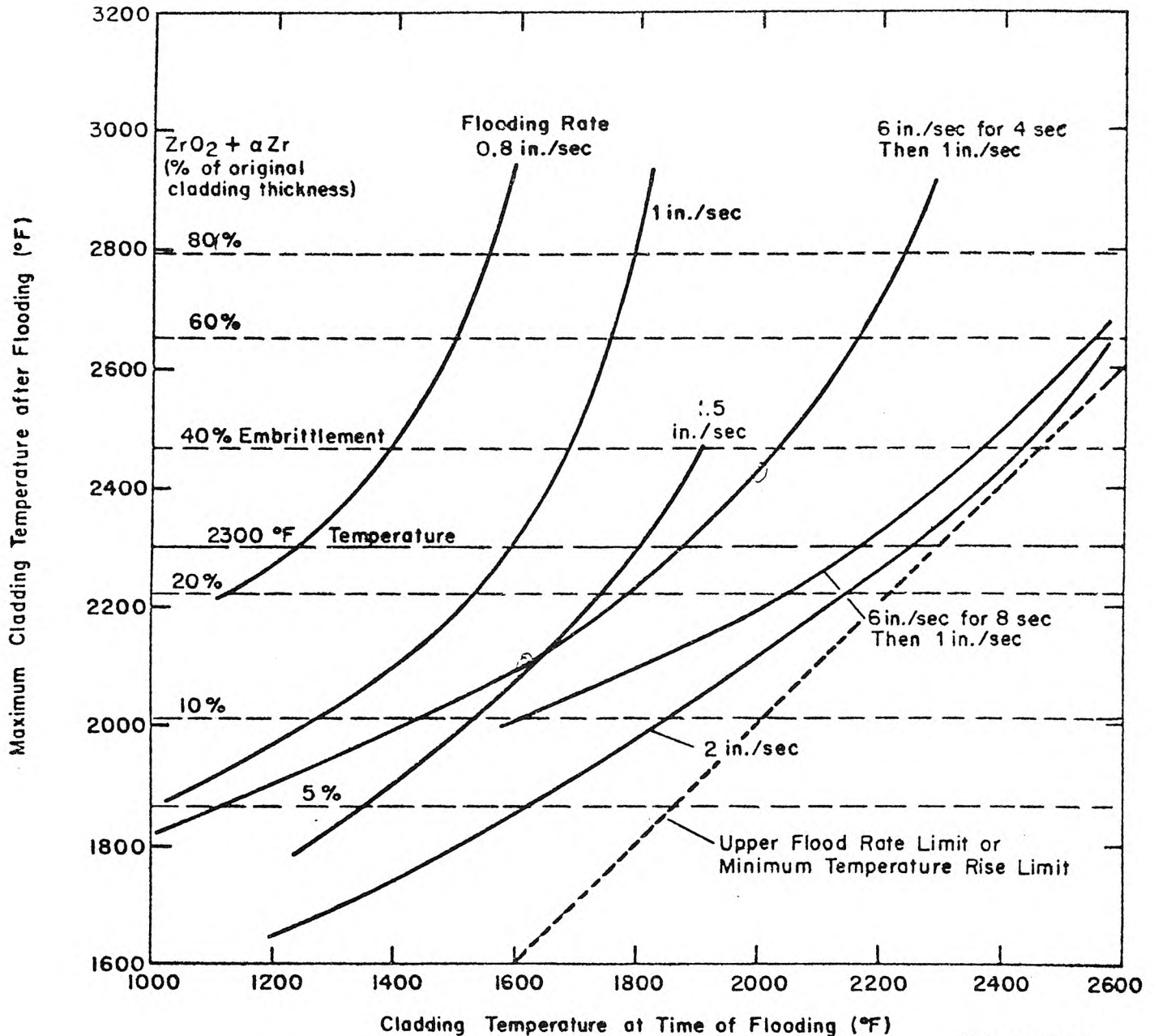
An independent series of parametric calculations conducted by ANC has been designed to provide estimates of the importance of various LOCA events and parameters (10, pp. 322-331).

Numerical calculations were made of fuel rod heat up based upon selected parameters for initial and decay power generation rates, physical properties of the fuel and zircaloy, an assumed gap conductance of 500 B/hr-ft²-°F, metal-water reaction rates, initial clad and fluid temperatures and surface heat transfer coefficients. For PWRs, the surface heat transfer coefficients were based upon PWR-FLECHT results as a function of reflooding rate. The results of the calculation showing representative peak clad temperatures for a PWR as a function of flooding rate and temperature at the initiation of flooding are shown in figure A10.7. Also calculated in the analysis, and shown in the figure, is the so-called "embrittlement" factor -- a calculated relative oxidation depth of ZrO₂+αZr. The embrittlement factor was obtained by applying a factor of 2.2 to the calculated (Baker-Just) equivalent ZrO₂ depth for the problem. The 2.2 conversion factor was derived on the basis of an empirically based evaluation of the relative depth ratio of the combined oxide and α-layers compared to the Baker-Just calculated oxide depth. On this basis, a 40 percent embrittlement factor is equivalent to a Baker-Just calculated relative oxidation depth of ZrO₂ of 18 percent of the original clad thickness.

Figure A10.7 shows, for example, that if the ECCS delivers coolant to the core at 1 in/sec, and if the core midplane temperature at the time of reflooding is 1500°F, then the maximum rod temperature will be 2200°F and the cladding will be 19 percent "embrittled". Similarly, the results imply that at the same flooding rate (1 in/sec) the predicted rod thermal response cannot be controlled if blowdown temperatures at the time of flooding exceed approximately 1800°F.

The lines of constant embrittlement of figure A10.7 have an interesting characteristic -- they are essentially horizontal. Thus, for the transients investigated, embrittlement was almost wholly a function of the maximum temperature. Since the time at temperature is

Figure A10.7
PWR-ECC Performance Map



ANC-A-861-H

Fig. 12 PWR-ECC performance map obtained through use of PWR-FLECHT data.

Reprinted, by permission, from Brockett, G.F., R.W. Griebel, R.W. Shumway, and J.O. Zane, "Loss of coolant: Control of Consequences by Emergency Core Cooling", Proceedings of the International Conference on Nuclear Solutions to World Energy Problems, Joint ANS/AIF Conference, November 13-17, 1972, Washington, D.C.

(10 and the American Nuclear Society)

an important factor in embrittlement, the results imply that the time histories of thermal excursions are essentially identical for all corresponding combinations of flooding rate and temperature at flooding initiation which result in a given peak temperature.

The relative importance of variations in some of the other parameters of the problem is shown in table A10.3. As described by the authors:

As an aid in estimating the effect of several other parameters, results from other sensitivity studies are presented in Table I [Table A10.3]. This table shows the percent change in the cladding temperature rise and embrittlement for two points in Figure 12. These points are for 2000 and 1600°F initial temperatures for a flooding rate of 6 in/sec for 4 seconds followed by a flooding rate of 1 in/sec. By utilizing the information in Table I and the curves of Figure 12 [Figure A10.7], new performance maps could be constructed.

The largest changes occur in the 2000°F column because the metal-water reaction energy is more significant at this temperature than for the 1600°F temperature at reflood initiation. The parameter which caused the greatest effect was the initial power. Next, in the order of importance, are: (a) metal-water reaction energy multiplying factor; (b) the time at which reflooding begins; and (c) ZrO₂ thickness.

Another important parameter affecting the relationship between the temperature at the time of reflooding initiation and the maximum temperature for a given reflooding rate is containment pressure. The containment pressure affects the flooding rate as well as the heat transfer for a specific flooding rate. As an example of this effect, if the core pressure is 25 psia instead of 60 psia and the reflooding rate is 1 in/sec, a 1500°F temperature at reflood initiation would result in a maximum temperature 500°F above the value obtained from Figure 12 [figure A10.7] (emphasis added) (10, pp. 325, 326).

The differences observed in the relative importance of various factors between the ANC results and the GE investigation are worth reviewing.

Table A10.3 ANS Sensitivity Study

TABLE I
SENSITIVITY STUDY SHOWING EFFECT OF PARAMETER CHANGES ON RESULTS IN FIGURE 12

Parameter Value	Temperature of 2000°F at time of reflooding		Temperature of 1600°F at time of reflooding	
	Temperature Rise (% Change)	Cladding Embrittlement (% Change)	Temperature Rise (% Change)	Cladding Embrittlement (% Change)
Baker-Just multiplication factor				
= 0.5	-21	-21	- 6	- 9
= 1.0	55	75	9	12
Initial power (kw/ft) = 1.0	-56	-50	-45	-61
= 1.4	106	170	53	101
ZrO ₂ thickness (in.) = 0.001	-13	- 9	- 9	- 8
= 0.00001	1	2	2	2
7-sec delay in time to initiate reflooding{a}	-16	-16	-10	-15

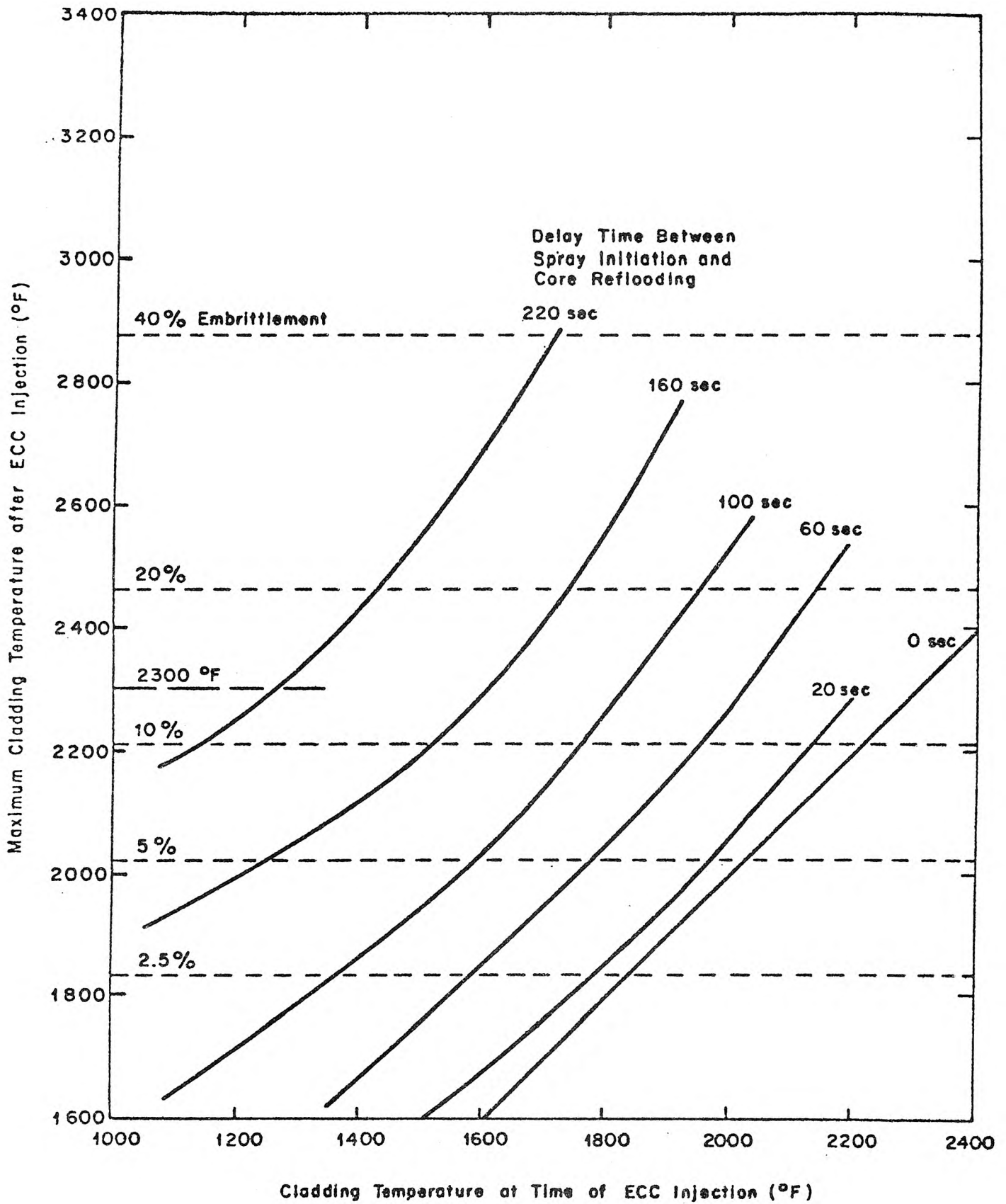
{a} Delays in flooding initiation result in a reduced temperature rise at any given temperature at reflooding initiation because the decay power is decreased.

Reprinted by permission from Brockett, G.F., R.W. Griebe, R. W. Shumway, and J.O. Zane, "Loss of Coolant: Control of Consequences by Emergency Core Cooling," Proceedings of the International Conference on Nuclear Solutions to World Energy Problems, Joint ANS/AIF Conference, November 13-17, 1972, Washington, D.C. , (10 and the American Nuclear Society.)

Note that the ANC study was restricted to investigation of factors affecting thermal response following blowdown (during the reflood phase) while the GE study considered the entire LOCA event. Essentially all of the GE factors of importance were related to blowdown factors and consequently could not appear in the ANC study, since blowdown related elements of the LOCA were not investigated. In analyzing the ANC results, it should also be noted that of the cited important parameters, both the metal-water reaction energy multiplying factor and ZrO_2 thickness are parameters influencing the metal-water energy input to the rod. This feature serves to support the earlier observation (appendix 7) of the importance of the metal-water reaction energy input to the thermal response of the system, as opposed to the AEC's view of the relative unimportance of this source of energy input to the system. However, it should also be noted that the AC (as well as the IAC) require the use of a Baker-Just multiplier of 1.0 -- a conservative estimate as indicated.

Results of a similar investigation by ANC of the BWR are shown in figure A10.8. The results give support to the critical role which GE has claimed for the single failure criterion. If auxiliary power is not lost and the delay time between spray initiation and core reflooding minimized, then the temperature rise is also reduced. As long as peak temperature remains below 2200°F, lines of constant delay between spray initiation and reflood are almost parallel. This implies that flooding temperature turnaround is almost instantaneous. This can be confirmed by comparison of constant delay curves with the zero second delay curve, which shows no temperature increase regardless of the initial cladding temperature at the time of ECC injection. The effects of the metal-water reaction are not particularly significant (less than 10 percent) for the transients with peak temperatures below 2200°F limits.

Figure A10.8



BWR Performance Map

(Reprinted by permission from 10 and the American Nuclear Society)

10.1.2 AEC parametric investigation

A third parametric investigation of particular importance was conducted by the AEC and presented in their Supplemental Testimony (4). Table A10.4 (Table 10.6 (revised) of 4) shows the results of this study in which the effects of variations in reflood heat transfer coefficients, blowdown heat transfer coefficients and gas gap conductances (Helium Conductivity Multiplier) were investigated. There is a great deal of interesting information to be deduced from this table on a variety of subjects.

Perhaps the most obvious result of the study was that rod temperatures reaching approximately 2100°F could not be controlled and were observed to reach melting under the influence of the metal-water reaction occurring when the fuel rods became swollen and ruptured, allowing oxidation on both inside and outside surfaces. The results indicate a pronounced metal-water reaction induced temperature instability at values lower than the criteria limits of either the IAC or PR. This result is shown by the large number of hash marks on the table which indicates that for cases marked in this fashion the "clad temperature reached melting."

The study investigated variations about a reference base case calculation which hypothesized a situation where fuel rods did not swell or rupture regardless of their limits of exposure. Variations about the base case were considered for three parametric rupture times: two occurring during blowdown (at 3 and 7 sec respectively) and a third variation which occurred after blowdown was completed, at 22.5 sec, at the beginning of the heatup period when the core was assumed to be uncovered and drying out. No convective film heat transfer was considered to take place during this period until reflood began, at approximately 34 sec.

It is interesting to observe that, except for a few variations involving post-blowdown swelling and rupture, melting occurred for

Table A10.4 AEC Sensitivity Study

Table 10.6

Study No. 2

Peak Cladding Temperature

Linear Power Density (kw/ft)			11.1	11.1	11.1	14.1	14.1	14.1	17.1	17.1	17.1
Rupture Time Seconds			22.5	7.0	3.0	22.5	7.0	3.0	22.5	7.0	3.0
fhr*	fhb**	fkf***									
.6	.5	.125	1577.1	1925.7	2012.0	****	-	-	-	-	-
.6	.5	.25	1587.8	1913.4	1975.7	-	-	-	-	-	-
.6	.5	.5	1605.0	1894.6	1931.9	-	-	-	-	-	-
.6	.5	1.0	1628.5	1869.7	1886.1	-	-	-	-	-	-
.6	1.0	.125	1577.1	1857.7	1912.5	-	-	-	-	-	-
.6	1.0	.25	1587.8	1838.1	1878.2	-	-	-	-	-	-
.6	1.0	.5	1605.0	1807.8	1831.8	-	-	-	-	-	-
.6	1.0	1.0	1628.5	1766.0	1775.8	-	-	-	-	-	-
.8	.5	.125	1362.2	1664.3	1699.2	1681.1	-	-	-	-	-
.8	.5	.25	1381.1	1664.2	1693.3	1698.1	-	-	-	-	-
.8	.5	.5	1409.2	1662.2	1682.8	1725.1	-	-	-	-	-
.8	.5	1.0	1443.8	1657.1	1668.4	1765.2	-	-	-	-	-
.8	1.0	.125	1362.2	1625.6	1657.4	1681.1	-	-	-	-	-
.8	1.0	.25	1381.1	1617.7	1664.0	1698.1	-	-	-	-	-
.8	1.0	.5	1409.2	1602.4	1620.5	1725.1	-	-	-	-	-
.8	1.0	1.0	1443.8	1576.2	1584.3	1762.0	2039.1	2074.8	-	-	-
1.0	.5	.125	1262.9	1574.6	1593.4	1522.4	2091.4	-	1925.0	-	-
1.0	.5	.25	1283.0	1572.7	1593.5	1541.1	2051.5	2355.3	1950.2	-	-
1.0	.5	.5	1316.3	1563.7	1584.3	1570.3	2004.2	2083.2	1993.7	-	-
1.0	.5	1.0	1356.4	1563.8	1556.8	1609.1	1955.9	1986.8	2060.2	-	-
1.0	1.0	.125	1262.9	1531.2	1540.8	1522.4	1919.5	2008.8	1925.0	-	-
1.0	1.0	.25	1282.9	1522.4	1535.1	1541.1	1889.0	1949.6	1950.2	-	-
1.0	1.0	.5	1316.3	1502.4	1514.9	1570.3	1844.8	1879.0	1993.7	-	-
1.0	1.0	1.0	1356.4	1489.4	1466.9	1609.1	1788.2	1801.6	2060.2	-	-
1.2	.5	.125	1262.9	1559.6	1593.4	1430.3	1874.3	1962.6	1722.2	-	-
1.2	.5	.25	1270.6	1565.3	1593.5	1442.7	1858.2	1924.6	1743.6	-	-
1.2	.5	.5	1282.6	1564.0	1584.3	1476.7	1836.3	1879.0	1778.2	-	-
1.2	.5	1.0	1297.7	1546.1	1556.8	1519.8	1815.6	1836.7	1826.4	-	-
1.2	1.0	.125	1262.9	1513.0	1540.8	1430.3	1775.3	1831.9	1722.2	-	-
1.2	1.0	.25	1270.6	1507.9	1535.1	1442.7	1752.9	1796.6	1743.6	-	-
1.2	1.0	.5	1282.6	1495.7	1514.9	1476.7	1721.4	1748.6	1778.2	-	-
1.2	1.0	1.0	1297.7	1457.5	1466.9	1519.8	1681.6	1692.9	1826.4	2256.4	2423.9

fhr* - Reflood Heat Transfer Coefficient Multiplier, applies to calculation only after rupture.

fhb** - Blowdown Heat Transfer Coefficient Multiplier, applies to calculation only after rupture.

fkf*** - Helium Conductivity Multiplier, applies to calculation only after rupture.

**** - Means Clad Temperature reached melting.

essentially all cases investigated where the initial power density was greater than 17.1 Kw/ft. For those cases where the effective reflood rates were less than one inch per second (i.e., reflood heat transfer coefficients less than those of the base case), when the initial linear power density was as low as 14.1 Kw/ft, melting also occurred for essentially all cases investigated. This is particularly significant since peak design linear power densities, for DBA estimation purposes, customarily are about 18-19 Kw/ft. These results imply that rather severe restrictions might have to be placed upon reactor operating powers to prevent excessive temperature excursions in the event of a LOCA -- assuming the validity of the AC required models for gap conductance in the presence of clad ballooning and rupture and the current low reflood rate predictions.

Considering the effects of gap conductance on blowdown and reflood heat transfer, the verification of some of the results discussed under the gap conductance discussion can be observed. When swelling and rupture occurred during blowdown, temperatures increased (the magnitude depending primarily upon the initial power density) as the gap conductances decreased. For such cases, gap conductances "ranged from 20-80 B/hr-ft²-°F" (4, p. 10-21). On the other hand, when swelling and rupture took place during reflood following blowdown (initial blowdown gap conductances were assumed to be high, on the order of 1000 B/hr-ft²-°F), peak temperatures were calculated to decrease on the order of 100°F with decreasing gap conductance (with a parametric variation over the same equivalent conductance range as the blowdown cases) in a manner exactly opposite to the cases where swelling and rupture occurred during blowdown.

This observation demonstrates the problem of defining what constitutes a conservative assumption with respect to the gap conductance parameter. For the cases where rupture occurred during blowdown, the low gap conductance restricted energy flow from the fuel elements resulting

in higher fuel temperatures. These higher temperatures were ultimately transferred to the cladding during reflood, a period of very poor convective heat transfer, with resulting increasing peak temperatures associated with decreasing gap conductances. In the post-blowdown swelling and rupture cases, apparently the higher gap conductances during blowdown permitted sufficient energy transfer during this early period so that fuel temperatures during the blowdown period were substantially lower than for those cases where swelling and rupture occurred early during blowdown. Thus, for these cases, when clad ballooning occurred during reflood, it resulted in a beneficial restriction of subsequent heat flow producing decreasing peak cladding temperatures as gap conductance decreased.

The influence of gap conductance is further demonstrated by an examination of the effects of rupture time changes. The principal heat transfer effect on the change in rupture time is associated with the gap conductance. As previously noted, the ballooned gap had a heat transfer coefficient of 20-80 B/hr-ft²-°F, while the non-ballooned cases had gap coefficients on the order of 500-1000 B/hr-ft²-°F. The results clearly indicated that such a reduction in gap HTC's had a profound effect on blowdown heat transfer. For the low power cases (11.1 Kw/ft), temperature increases of from approximately 100°F to 400°F are associated with early ballooning (3 or 7 sec) when compared with temperatures of those cases where rupture was delayed until after blowdown was complete (the 22.5 sec cases). At low power, the effect of reflood rate upon rupture time induced incremental peak temperature changes was not significant. As long as peak temperatures were kept within reasonable limits, less than 2000°F, results were reasonably consistent -- even though peak temperatures increased over 500°F, while the reflood rate was reduced by a factor of two.

However, for the relatively higher powered cases (14.1 and 17.1 Kw/ft), nonlinearities appeared in the incremental peak temperatures

induced by reducing rupture times. For these cases, melting occurred for all cases where the flooding rate HTC was less than nominal (1 inch per sec). At the high power levels (17.1 Kw/ft), even at the highest simulated reflood rates, no reduction in rupture time was feasible without melting. For these cases, if meltdown was to be avoided at all, the high initial gap conductances were necessary to avoid disaster. But even with high gap conductances, meltdown occurred for all cases where reflood rates were less than nominal.

Considering the effect of reflood rates on peak temperatures, table A10.4 also indicates that at low power (11.1 Kw/ft) the effects were reasonably uniform irrespective of the conditions of the rods (i.e., whether they had undergone early ballooning or not). For example, a 20 percent reduction in reflood HTC (from nominal to 0.8 nominal) produced incremental changes in peak temperature of about 100°F (a fractional increment on the order of 6%) regardless of ballooning conditions, decreased gap conductance, or reduced blowdown heat transfer.

The borderline nature of the nominal reflood HTC (associated with 1 in/sec reflooding rates) to control the temperature excursion, is exemplified in the results of an additional 20 percent decrease in reflood HTC (from 0.8 to 0.6 nominal). Under these circumstances, peak temperatures increased incrementally about 200 to 300°F (a change of approximately 15 percent). For these cases, the peak temperatures were barely held beneath the critical 2000°F levels at the lowest power levels investigated (11.1 Kw/ft).

Comparing the two sets of cases (i.e., the transitions from 1.0 to 0.8 nominal with that from 0.8 to 0.6 nominal), nonlinearities in peak temperature increments are evident. The nonlinearities in incremental peak temperatures indicate problems associated with control of peak temperature as the temperature increases toward 2000°F. As peak temperatures approach the critical 2000°F level, incremental changes

in temperature in excess of 300°F can be observed in comparison to the 100°F increments at the lower temperature levels. Such nonlinearities are characteristics of the influences of each of the pertinent variables as peak temperatures approach the 2000°F level.

As previously observed, the effectiveness of reflooding heat transfer is sharply reduced as linear rod power is increased. At the intermediate power levels (14.1 Kw/ft) reflooding effectiveness is severely compromised by small perturbations in blowdown heat transfer and gap conductance. For the early rupture cases (at 3 or 7 sec), temperatures increase from about 1700°F to nearly 2000°F as blowdown heat transfer and gap conductance are reduced, at even the highest reflood heat transfer conditions investigated (i.e., 1.2 nominal).

Generally speaking, at low power levels, blowdown heat transfer was the least sensitive parameter investigated. Reducing blowdown HTC by 50 percent produced 50 to 100°F increases in peak temperatures (a 2 to 6 percent temperature increase). At these low power levels, the effect of changes in blowdown HTC were relatively insensitive to variations in other parameters, including temperature. However, when linear rod power levels were raised to 14.1 Kw/ft, or higher, blowdown HTC became as important a parameter as any of the others investigated. Temperature increases of from 100 to 400°F (6-10 percent) were observed for 50 percent reductions in blowdown HTC at the 14.1 Kw/ft power level.

In summary, the AEC parametric study shows that at low power levels, relatively minor perturbations in any of the parameters were shown to be tolerable, producing about 50 to 100°F changes in peak temperatures.

However, large perturbations in parameters, such as (1) major gap conductance decreases produced through early rupture time, or (2) changes in linear rod power levels, produced important changes in peak temperatures (from 100 to 500°F). Such changes were barely tolerable

under the most ideal conditions, and were fundamentally intolerable under essentially all conditions investigated which were off normal (or were otherwise non-ideal). At rod power density levels greater than 11.1 Kw/ft (considerably below current design peak linear rod power densities of 18-19 Kw/ft), meltdown occurred at essentially all off-normal operating conditions investigated. Moreover, the results presented indicate that the thermal response of the rods is a strongly non-linear function of temperature. As peak temperatures approach 2000°F, normally minor perturbations in heat transfer related variables induce temperature excursions which are increasingly difficult to control. This appears to be directly related to energy input to the system from metal-water reactions at about 2000°F and above. Under such circumstances, the nominal reflood heat transfer rates (one inch per second) are stressed to their limits. In fact, it appears that under design basis accident power conditions currently anticipated (18-19 Kw/ft), rates will be inadequate to assure that meltdown will not occur over a relatively large fraction of the core, assuming the basic accuracy of the AEC's parametric study.

Vendors have objected to using the AEC analysis methods judged to be unrepresentative of the thermal excursion over the entire core. They point out that swelling and rupture are very localized phenomena -- an inch or two on a 12 ft rod -- as are the maximum DBA peaking factors associated with the 18-19 Kw/ft linear rod power densities. Consequently they feel that applying the results of such a single "hot rod" calculation to the entire core is very conservative.

This is probably true. However, the AEC does not currently recognize the adequacy of any of the statistical models which are used to estimate the distribution of peak linear power density or swelling and rupture over the core. Consequently they feel that there is no way of accurately predicting how extensive the melting might be. Thus,

though the results may be very conservative and damage resulting to the core from this mechanism relatively small, in actual practice the AEC has prescribed that this conservative method be applied in the AC.

10.1.3 Ranking critical parameters

The most significant observation to be deduced from the previously cited investigations is that ranking critical parameters is not easy. The ranking of the parameters depends, to a large extent, on the limits of the range of parameter variations which were selected as "realistic" in the various studies conducted. To the extent that they may have been selected unrealistically, especially if they were chosen to support the position that the IAC was excessively conservative, we are currently at the mercy of those who have conducted the investigations.

Recognizing these limitations, the results seem to imply that blowdown heat transfer parameters are very important. Critical heat flux (CHF) parameters are especially important. This parameter affects the initial duration of nucleate boiling and the potential reestablishment of high heat transfer boiling conditions throughout blowdown (evidently an especially important period) and reflood periods. In a related manner, the critical break flow rate has been shown to be significant in its influence on the time to DNB and the time history of fluid availability for cooling during blowdown.

Obviously, the initial power and the decay heat release are important driving functions of the system. As shown in table A10.3 from the ANC study, a 20 percent increase in power resulted in a corresponding increase in temperature of 53 percent or more, even when reflooding started at the relatively cool blowdown temperature of 1600°F. This observation serves to highlight the critical nature of the question of the validity of the ANS Standard 5.1 + 20 percent decay heat criterion as well as the potential results of limiting (or reducing) peak operational power for the facilities.

The ANC study also highlights the importance of the metal-water reaction energy release rate. Though the energy release from metal-water reactions may not have been given proper recognition as a significant LOCA energy source in the AEC publications defending the ECCS criteria, it has been treated conservatively in both the IAC and the revised AC. The use of the full Baker-Just relationships for the energy release rate in calculations should conservatively predict this energy source.

This LOCA parameter evaluation has, of necessity, been rather qualitative. Development of a valid statistical basis for probabilistic evaluation of thermal excursions of the type conducted by GE would be a valuable contribution to the resolution of ECCS uncertainties. It would also be enlightening to have the results of peak temperature differences associated with observations for thermal excursions corresponding to pessimistic branches of a LOCA fault tree as well as the optimistic branches selected by GE (e.g., figure A10.3).

Though vendors, in general, have all indicated that they felt the IAC requirements led to excessive conservatism in design, the AEC parametric results shown in table A10.4 do not appear to support this contention. The results shown indicate that reflood rates of one inch per second are of borderline adequacy in controlling thermal excursions where swelling and rupture of rods takes place early in the blowdown. They also indicate that for large portions of the core, where linear power densities are greater than 11.1 Kw/ft, small adverse perturbations to current estimates of projected ECCS operating conditions may result in meltdown. All variables including reflood and blowdown heat transfer, gap conductivity, rupture time and operational power densities were shown to be important contributors to thermal excursions. However, major changes in gap conductance through early rupture time and perturbations in linear power density as well as reflood heat transfer were shown to dominate the heat transfer mechanisms.

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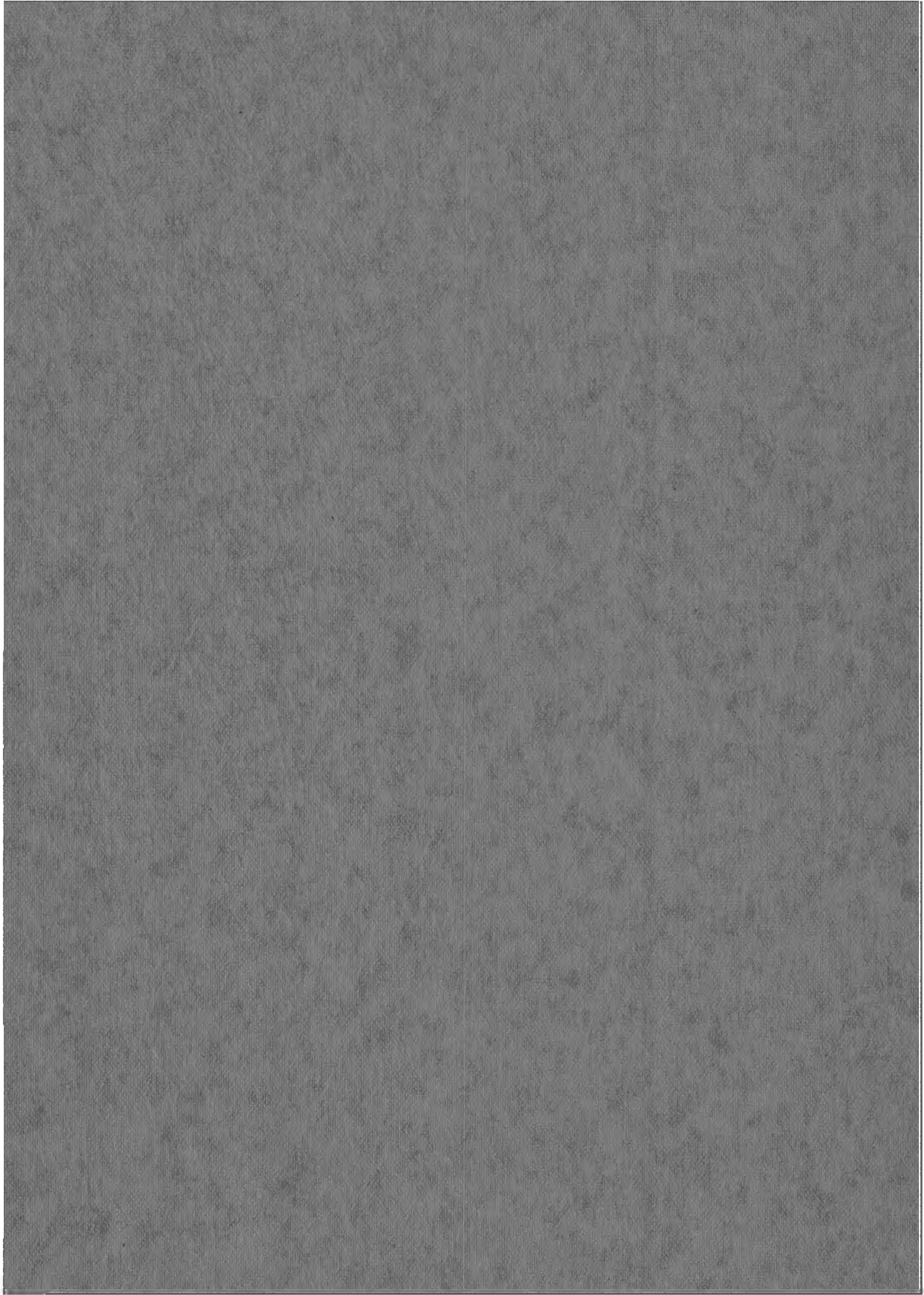
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